

VISIT...

LANZAROTE
Caliente.COM

6

Automotive Claw-Pole-Rotor Generator Systems

6.1	Introduction	6-1
6.2	Construction and Principle.....	6-2
6.3	Magnetic Equivalent Circuit (MEC) Modeling	6-6
6.4	Three-Dimensional Finite Element Method (3D FEM) Modeling	6-9
6.5	Losses, Efficiency, and Power Factor.....	6-14
6.6	Design Improvement Steps.....	6-17
	Claw-Pole Geometry • Booster Diode Effects • Assisting Permanent Magnets • Increasing the Number of Poles • Winding Tapping (Reconfiguration) • Claw-Pole Damper • The Controlled Rectifier	
6.7	The Lundell Starter/Generator for Hybrid Vehicles.....	6-24
6.8	Summary.....	6-32
	References.....	6-33

6.1 Introduction

Increasing comfort and safety in cars, trucks, and buses driven by combustion engines require more installed electric power on board [1]. As of now, the claw-pole-rotor generator is the only type of automotive generator used in industry, with total powers per unit up to 5 kW and speeds up to 18,000 rpm.

The solid rotor claw-pole structure with ring-shaped single direct current (DC) excitation coil, though supplied through slip-rings and brushes from the battery on board, has proven to be simple and reliable, with low cost, low volume and low excitation power loss.

In general, the claw-pole-rotor generator is a three-phase generator with three or six slots per pole and with 12, 14, 16, 18 poles, and a diode full power rectifier.

Its main demerit is the rather large losses (low efficiency), around 50% at full power and high speed. Producing electricity on board with such high losses is no longer acceptable as the electric power requirements per vehicle increase.

Improvements to the claw-pole-rotor (or Lundell) generator design for better efficiency at higher powers per unit are currently under aggressive investigation by both industry and academia, and encouraging results were recently published.

The first commercial mild hybrid electrical vehicle, launched in 2002, makes use of the Lundell machine as a starter/generator.

This chapter presents both simplified and advanced modeling of the Lundell generator for steady-state and transient performance. Pertinent control and the latest design improvement efforts and results are also included. The chapter ends with discussion of the starter-generator mode Lundell machine, with its control and design aspects for hybrid electric vehicles.

6.2 Construction and Principle

A cross-section of a typical industrial claw-pole-rotor generator is shown in Figure 6.1. It contains the following main parts:

- Uniformly slotted laminated stator iron core
- Three-phase alternating current (AC) winding: typically one layer with $q = 1, 2$ slots per pole per phase, star or delta connection of phases
- Claw-pole rotor made of solid iron parts that surround the ring-shaped DC-fed excitation (single) coil
- Copper slip-rings with low voltage drop brushes to transfer power to the DC excitation coil on the rotor
- Bearings and an end frame made of two sides — the slip-ring side and the drive-end side; the generator is driven by the internal combustion engine (ICE) through a belt transmission

The Lundell generator AC output is rectified through a three- or four-leg diode rectifier and connected to the on-board battery (Figure 6.2). Today, 14 V_{dc} batteries are used, but 42 V_{dc} batteries are now adopted as the new standard for automotive application loads [1].

The diodes D_1 to D_6 (D_8) serve the full-power output rectification and are designed for the maximum power of the generator. For large units (for trucks, etc.), three elementary diodes in parallel are mounted on radiator semilegs to comply with the rather high current levels involved (28 V_{dc} batteries are typical for large vehicles).

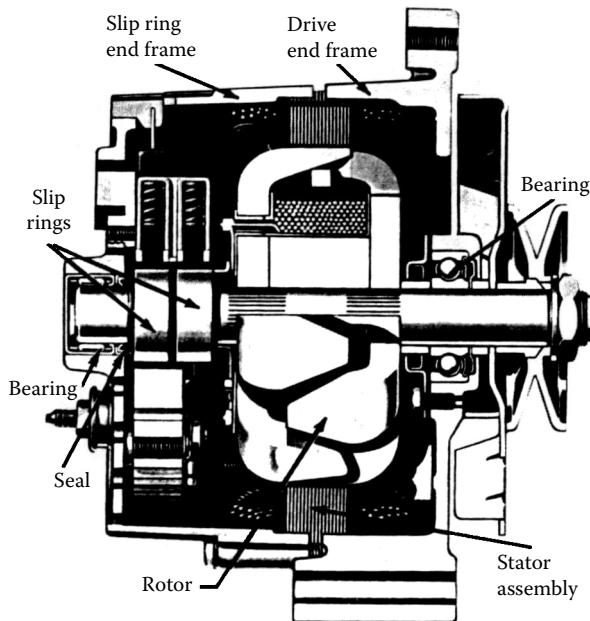


FIGURE 6.1 Claw-pole-rotor (Lundell) generator.

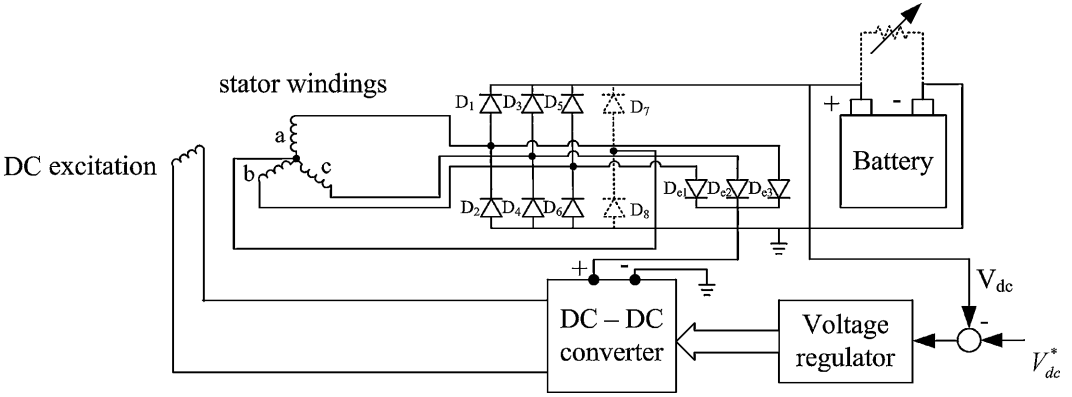


FIGURE 6.2 Typical industrial Lundell generator system.

The excitation coil is supplied from the generator terminals through a half-bridge diode rectifier of low current level (in the 5 to 20 A interval) and a DC–DC static power converter. A voltage sensor and regulator command the DC–DC converter to keep the voltage of the battery in a certain interval (roughly 12 to 17 V_{dc} , for 14 V_{dc} batteries) at all times, and provides overvoltage and overcurrent protection.

The battery voltage depends on the state of charge, on its ambient temperature, and on the load level. In designing the generator, the extreme conditions for the battery have to be considered. When the temperature decreases, the battery voltage increases for all states of battery charge. For 100% battery charge, the battery voltage may increase from 13.4 V at 60°C to 16 V at 20°C. For 10% battery charge, the same voltages are, respectively, 10.75 V and 14 V. Battery age plays an important role in voltage regulation. Comprehensive modeling of the battery is required to exploit it optimally, as the average life of a battery is around 5 yr or more for a typical car of today.

The generator should provide the same current for the load (or for the battery charge) from the ICE idle speed (700 to 1000 rpm, in general) onward. The excitation circuit is disconnected when the ICE is shut down, to save in battery life and in fuel consumption.

The stator windings for cars, especially, are the one-layer type, with $q = 1$ slot/pole/phase and with diametrical coils. They are machine-inserted in the slots, and the slot filling factor K_{fill} is modest (around or less than 0.3 to 0.32).

Only with large power units ($P > 2.5$ to 3 kW), $q = 2$, when chorded coils are used, to reduce magnetomotive force (mmf) first-space harmonics (fifth and seventh), the distribution and chording factors, for the γ^{th} harmonic, $K_{d\gamma}$, $K_{y\gamma}$, are as follows:

$$K_{d\gamma} = \frac{\sin \gamma \frac{\pi}{6}}{q \sin \gamma \frac{\pi}{6q}}; \quad K_{y\gamma} = \sin \gamma \frac{y}{\tau} \frac{\pi}{2} \quad (6.1)$$

For $q = 1$, chording the coils to $y/\tau = 2/3$, as the only possibility, the mmf fundamental (or power) is as follows:

$$(K_{y1})_{\frac{y}{\tau} = \frac{2}{3}} = \sin \frac{2}{3} \frac{\pi}{2} = 0.865 \quad (6.2)$$

This is why chording is not generally used for $q = 1$, though the length of coil end turns would be reduced notably, and so would the stator winding losses.

The number of poles is, in general, $2p_1 = 12$, as a compromise between size reduction and increasing iron core loss. Also, $2p_1 = 14, 16, 18$ are used for larger power units (for buses, trucks, etc.).

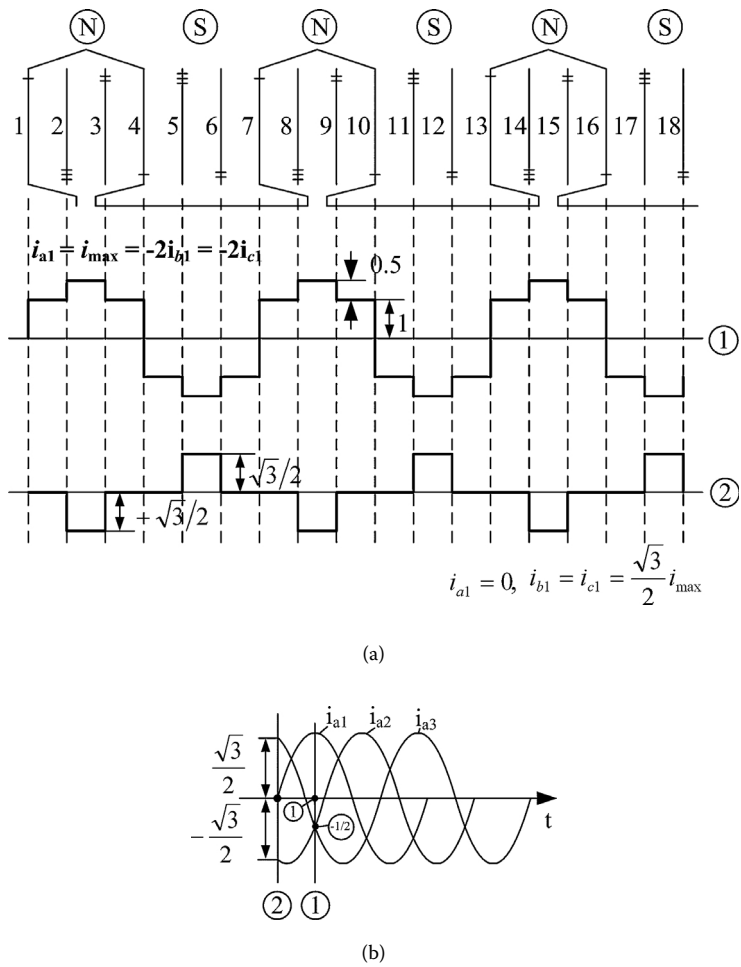


FIGURE 6.3 (a) The 36-slot, 12-pole winding (half of it is shown) and (b) the stator magnetomotive force (mmf) distribution for three and two conducting phases.

For completeness, let us include here the typical three-phase, $N_s = 36$ slots, $2p_1 = 12$ poles winding (Figure 6.3a and Figure 6.3b). Only one phase is shown in slots. The mmf distributions for sinusoidal currents for $i_{a1} = i_{max} = -2i_{b1} = -2i_{c1}$ and for $i_{a1} = 0$, $i_{b1} = i_{c1} = -i_{max} \frac{\sqrt{3}}{2}$ are also shown.

The mmf distribution changes between the extreme shapes in Figure 6.3a. With the diode rectifier, the waveform of the stator phase currents changes from quasi-rectangular (discontinuous) at idle engine speed to quasi-sinusoidal (continuous) waveform at higher speeds (Figure 6.4).

For the eight-diode bridge, the existence of null current leads to a third harmonic in the phase currents. So, even without considering the magnetic saturation, the mmf space harmonics and phase current time harmonics pose specific problems to the operation of the Lundell machine, which is, otherwise, a salient-pole rotor synchronous machine.

Typical phase voltage and current waveforms at 1500 and 6000 rpm are shown in Figure 6.5a and Figure 6.5b, with rectifier and resistive load (no battery).

As the speed increases notably, third harmonics show up in the phase voltage. Also, the phase shift between the fundamental voltage and current increases from about 1° at 1500 rpm to 9° at 6000 rpm. This latter phase lag is categorically due to the commutation of diodes — three diodes work at a time at high speeds — in corroboration with the machine commutation inductances.

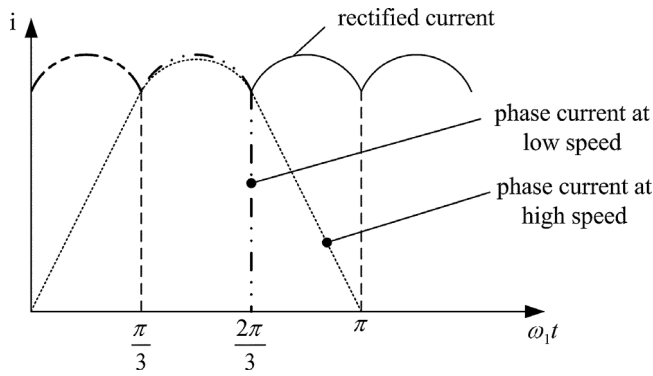


FIGURE 6.4 Ideal shapes of generator currents (star connection).

The third harmonic in the phase voltage under load comes from the distribution of the flux density in the airgap. This, in turn, is due to magnetic saturation in corroboration with the $q = 1$ diametrical winding and slot openings.

The trapezoidal shape of the claw poles (Figure 6.6) corresponds to a kind of double skewing and thus produces some reduction in flux density and radial force harmonics. Noise is reduced this way also, and so is the axial force.

To further reduce the noise, the claw poles have a chamfer (Figure 6.6). The chamfer also reduces the excitation field leakage between neighboring poles.

With τ the pole pitch, the same in the stator and rotor, the claw-pole span in the middle of stator stack, $\alpha_c \tau$, is the main design variable for claw poles. The additional geometrical variables are (Figure 6.6) ϕ_c , W_1 , W_2 , d_c , and W_c .

In general, the pole angle $\alpha_c = 0.45$ to 0.6 ; the claw angle is $\phi_c = 10$ to 20° . The lower values of α_c and ϕ_c in the above intervals tend to produce higher output with rectifier and battery operation.

Taking note of the above peculiarities of the Lundell generator, we can make a few remarks:

- An analytical constant parameter treatment, with only the fundamental phase voltage and current considered, should be used only with extreme care, as it is prone to large errors due to magnetic saturation at low speeds and due to armature-reaction flux density caused distortion at high speeds.

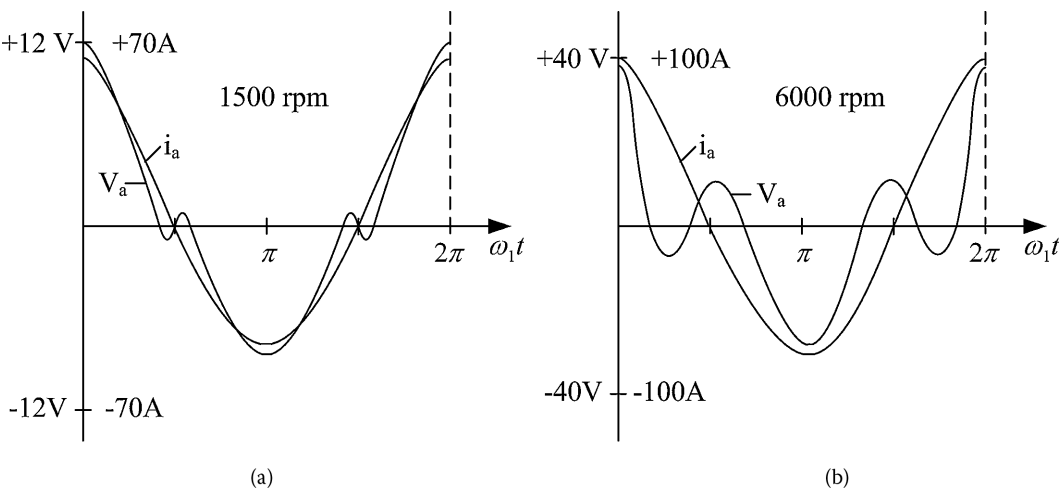


FIGURE 6.5 Phase voltage and current with diode rectifier and resistive load and same field current: (a) 1500 rpm and (b) 6000 rpm.

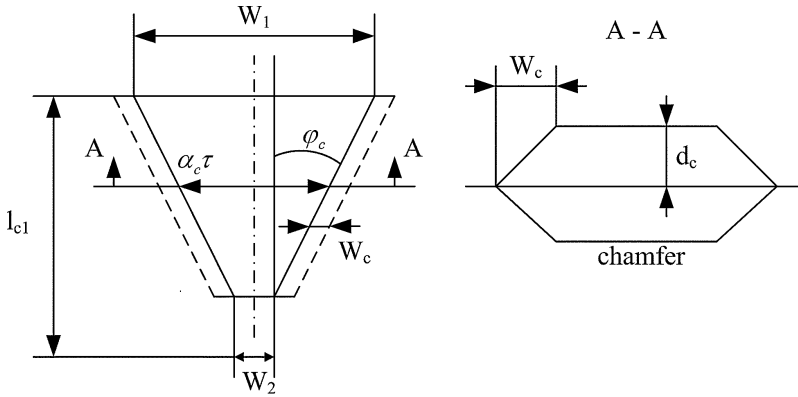


FIGURE 6.6 The rotor claw with chamfer.

- Including magnetic saturation only by equivalent saturation factors pertaining to fundamental flux distribution may not produce practical enough third harmonic values at high speeds (in Figure 6.5b: $V_3 = 9.71$ V and $V_1 = 7.21$ V at 6000 rpm).
- For the eight-diode rectifier, the phase voltage third harmonic in the phase current occurs. The null current is three times the third-phase harmonic current.
- In this case, applying the fundamental circuit model brings a bit more realistic results, but still, the third harmonic current has to be calculated separately, and its losses are included when efficiency is determined.

Consequently, a nonlinear, iterative circuit model is required to properly describe Lundell generator performance over a wide range of speeds and loads.

Conversely, a nonlinear field model could be used.

Three-dimensional finite element method (FEM) or magnetic equivalent circuit (MEC) modeling was applied to portray Lundell generator performance — steady state and transients — with remarkable success. The MEC approach, however, requires at least two orders of magnitudes (100 times) less computer time. Both these field methods will be presented in some detail, as applied to the Lundell generator, in what follows.

For voltage control design, however, an equivalent circuit model is required. Even the fundamental electric circuit with magnetic saturation coefficients along the two orthogonal axes should do (after linearization) for such purposes.

Composite FEM circuit models are also a way out of difficulties with Lundell generator steady-state performance computation.

6.3 Magnetic Equivalent Circuit (MEC) Modeling

The construction of the MEC should start with observation of a typical main magnetic flux path. Such a three-dimensional path is shown in Figure 6.7a and Figure 6.7b. It corresponds to no-load conditions (zero stator current). Each flux path section is characterized by a magnetic reluctance:

- Airgap: R_g
- Stator tooth: R_{st}
- Stator yoke: R_{sy}
- Claw, axial: R_{ca}
- Claw, radial: R_{cr}
- Rotor yoke: $2R_{ry}$

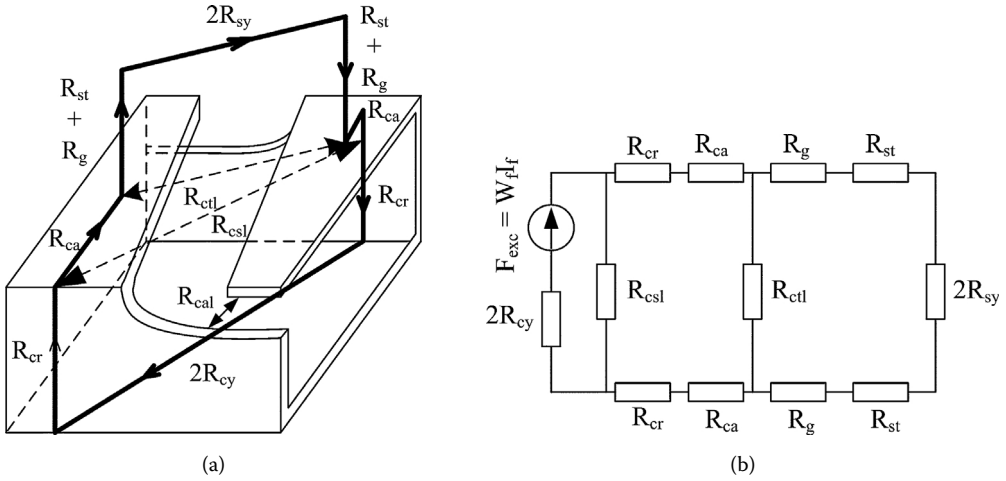


FIGURE 6.7 (a) Typical field (magnetomotive force [mmf]) flux line and (b) simplified magnetic equivalent circuit (MEC).

Besides, there are at least two essential leakage paths for this main flux line: one tangential and the other axial between claws: R_{ctl} and R_{cal} . In addition, there is the “slot” leakage magnetic reluctance of the ring-shape DC excitation coil: R_{csl} .

A simplified MEC for no load is shown in Figure 6.7b, where the axial interclaw leakage flux R_{cal} was neglected, for reluctance simplicity.

Analytical expressions for all these magnetic reluctances have to be adopted. While for the stator sections (R_{sy} , R_{st}) and the airgap (R_g) the formulae are rather straightforward (with zero slot openings), for the rotor sections (R_{cr} , R_{ca} , R_{cy} , R_{ctl}), the magnetic reluctance expressions, including magnetic saturation effects, are cumbersome, if good precision is expected, because the claw pole cross-section varies axially and so does the magnetic permeability in them. See for details Reference [2], pp. 125–127.

Acceptable no-load magnetization curves may be obtained this way:

$$E_1(I_f) = \pi \sqrt{2} \cdot n \cdot p_1 \cdot W_1 \cdot \Phi_{p1}(I_f); \quad \Phi_{p1} = B_{g1} \cdot \frac{2}{\pi} \cdot \tau \cdot l_{stack} \quad (6.3)$$

where

Φ_{p1} is the fundamental of stator polar flux ($q = 1$)

E_1 is the root mean squared (RMS) value of the electromagnetic force (emf) fundamental

τ is the pole pitch

l_{stack} is the stator stack length

n is the speed in revolution per sec (r/sec)

The relationship between the polar flux Φ_{p1} and the field current has to account for the approximated airgap flux density distribution at no load (Figure 6.8).

The rectangular distribution, calculated with the average rotor claw span $\alpha_p = 0.45$ to 0.6 (Figure 6.8) leads to an ideal flux density fundamental in the airgap B_{g1} of the following:

$$B_{g1} = \frac{4}{\pi} \cdot B_g \cdot \sin \alpha_p \cdot \frac{\pi}{2} = K_{\alpha_p} \cdot B_g \quad (6.4)$$

$$\Phi_{p1} = K_{\alpha_p} \cdot \Phi_p \quad (6.5)$$

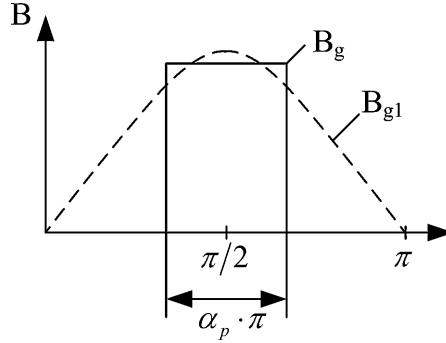


FIGURE 6.8 Ideal no-load airgap flux density.

Reducing the field current I_f to the stator I'_f is also required for the machine fundamental equation:

$$W_f \cdot I_f \cdot K_{\alpha_p} = 2 \times F_{10}; \quad F_{10} = \frac{3 \cdot W_1 \cdot I'_f \cdot \sqrt{2}}{\pi \cdot p_1} \quad (6.6)$$

F_{10} is the mmf fundamental per stator pole of a three-phase current I'_f that produces the same airgap field as the DC field current I_f :

$$I'_f = I_f \cdot K_{if}; \quad K_{if} = \frac{W_f \cdot K_{dp}}{6 \cdot W_1 \cdot \sqrt{2}} \quad (6.7)$$

$$\pi \cdot p_1$$

In general, to limit the DC excitation losses, the airgap is kept small ($g = 0.35$ to 0.5 mm), but, to reduce the machine reactances, and volume, the magnetic circuit is saturated for rated field current. Also, the solid-iron claw poles serve as a damper winding during transients and during commutation in the rectifier.

However, airgap field harmonics produce additional eddy currents in the claw poles even during steady state. These additional losses increase with speed up to a point and pose a severe limitation on machine output.

In the above approximations, the stator slot openings were neglected. To a first approximation, they may be considered by increasing the airgap magnetic reluctance R_g per pole by the known Carter coefficient — $K_c > 1$ [3]:

$$R_g \approx \frac{1}{\mu_0} \cdot \frac{g \cdot K_c}{\alpha_p \cdot \tau \cdot l_{stack}} \quad (6.8)$$

The Carter coefficient produces a rough approximation that is also global in the sense that the tangential actual distribution, strongly disturbed by the stator slot openings, is not visible.

For no load, however, the emf measured harmonics are smaller than 10%, even for rated field current, when heavy magnetic saturation occurs, which tends to “induce” a third harmonic in the emf. This saturation-produced third harmonic may be interpreted as if the inverse airgap function contains a second harmonics, besides the constant value.

While the above MEC is suitable for no load, a more complicated one, to take care of local magnetic saturation, an inverse airgap function variation with rotor position and along axial direction (due to the tapering shape of claws) is needed. Such a comprehensive model is presented in Reference [4], where the on-load operation is considered directly.

It is a three-dimensional model in the sense that the permeance of airgap varies along axial and tangential directions, while the stator and rotor permeances vary along radial and tangential directions. Axially and in the radial plane, the areas are divided into a few sections of almost uniform (but different) permeability (in iron). The more elements that are considered, the better precision is obtained, but the computation time increases notably. The magnetic permeance approximations are, in general, analytical functions of rotor position (of time).

Each slot and tooth is modeled by one (or two) element. Axially, the machine is divided into a few sections. The connection of phases is arbitrary.

With a few hundred elements, a comprehensive MEC can be built [5], with the voltage–current relationship added for completeness.

Remarkably good agreement between calculated and measured phase and neutral (for eight-diode rectifier) current is obtained with a few minutes of time on a laptop computer [4].

In the same time, the distribution of airgap flux density along circumferential directions is properly, though approximately, simulated.

6.4 Three-Dimensional Finite Element Method (3D FEM) Modeling

Though requiring much more computer time — intensive, three-dimensional — the FEM can capture the shear complexity of the flux lines in a Lundell generator at load, including magnetic saturation, claw-pole tapping, claw chamfering, and slot openings [6].

The periodicity conditions allow us to simulate only one pole (Figure 6.9). This way, the computation time for the 3D FEM becomes reasonable, though still high (hours for complete steady-state characteristics at a given speed).

Typical radial airgap and flux density distributions obtained through 3D FEM are shown in Figure 6.10a and Figure 6.10b [6].

The influence of slot openings is evident. The strong departure of the airgap flux density from a sinusoid is due to the low number of slots (three) per pole and to magnetic saturation (over the whole pole at no load and over half of it on load).

On load, the stator mmf at 6000 rpm is only slightly dephased with respect to the rotor longitudinal (pole) axis, as indicated by the severe demagnetization in axis *d* (at 180° in Figure 6.10b). Still, there is a rather high peak of airgap flux density at about 140° electrical, as expected.

The distortion in the total (resultant) emf is due to this mix of causes: three slots per pole and local magnetic saturation, dependent on load current and speed. The influence of speed comes into play through the stator mmf wave angle with the rotor poles, which tends to decrease to low values at high speed when the diode rectifier battery imposes active-power-only transfer from generator at all speeds. The small power factor angle increasing with speed, from 0° to 9° to 10°, is due to the reactive power “consumption” of machine inductances during diode commutation.

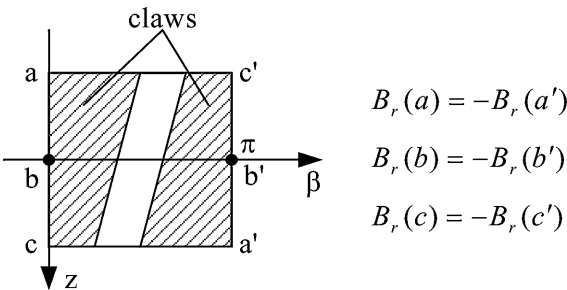


FIGURE 6.9 Symmetry conditions.

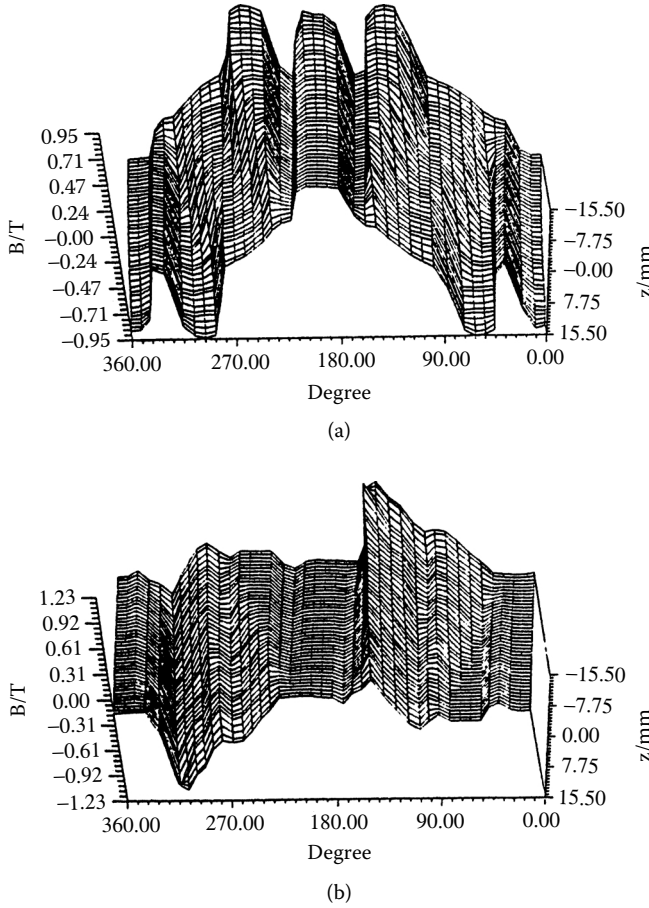


FIGURE 6.10 Radial airgap flux density distribution (Z is the axial variable): (a) on no load and (b) at 6000 on load.

FEM application needs the values of the phase currents for a given rotor position. However, to calculate these values, a circuit model of the machine, including the diode rectifier and the battery, is necessary. An iterative circuit model adopted through results from FEM is the obvious choice for the scope.

Calculating the operation point — for given speed, DC load (resistance), and field current — may be done by first approximating the relationship between the fundamental phase current and voltage V_1 , I_1 and their DC battery correspondents V_{dc} , I_{dc} .

With zero losses in the diode rectifier,

$$\begin{aligned} I_{dc} &= K_i \cdot I_1; \quad K_i = \frac{3\sqrt{3}}{\pi} \approx 1.35 \\ V_{dc} &= \frac{3V_1 \cdot I_1}{I_{dc}} = K_v \cdot V_1; \quad K_v \approx \frac{\pi}{\sqrt{2}} = 2.22 \end{aligned} \quad (6.9)$$

These approximations hold for the six-diode rectifier and star phase connection.

For AC loads, the situation is simpler, as the load resistance and reactance are given, and we need to calculate the output voltage and current fundamentals.

The phase current is considered essentially sinusoidal (star connection) for simplicity, though, at low speeds, it tends to be rectangular — discontinuous.

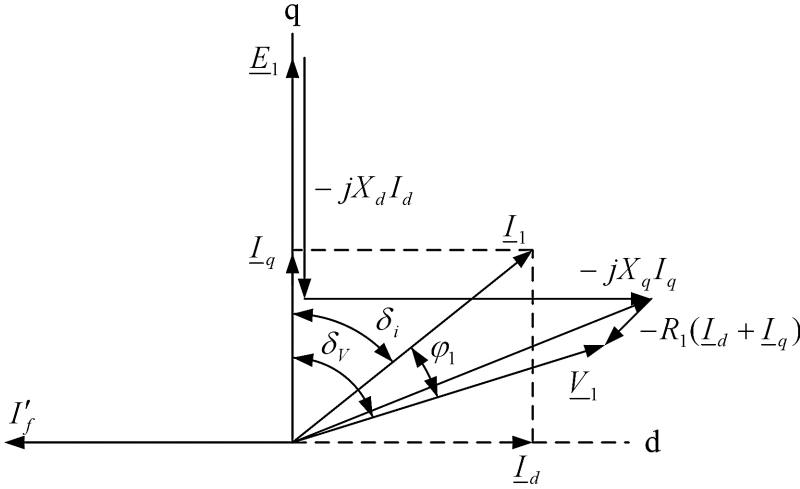


FIGURE 6.11 The phasor diagram.

The phasor diagram may be used in the iterative procedure. The d - q model equations — valid for fundamental quantities only — of the Lundell (synchronous) machine are as follows:

$$\underline{I}_1 R_1 + \underline{V}_1 = \underline{E}_1 - jX_d \underline{I}_{d1} - jX_q \underline{I}_{q1}; \quad \underline{I}_1 = \underline{I}_d + \underline{I}_q \quad (6.10)$$

$$\underline{E}_1 = -jX_{dm} \underline{I}_f'; \quad X_d = X_{dm} + X_{l1}; \quad X_q = X_{qm} + X_{l1} \quad (6.11)$$

X_{dm} , X_{qm} , X_{l1} are, respectively, magnetization synchronous and leakage stator reactances in the d - q model. (See Chapter 4 in *Synchronous Generators* on synchronous generators, steady state.)

For a small power factor angle, the phasor diagram corresponding to Equation 6.10 and Equation 6.11 is illustrated in Figure 6.11. The small ($\phi_1 < 0$) power factor angle takes into account the effect of diode commutation on reactive power absorption from the generator.

The iterative computation process starts with given phase current $\underline{I}_1$ and current power angle δ_i . (δ_i increases with speed, in general, for constant DC output voltage.)

Making use of FEM, the airgap flux distribution with its fundamental and space harmonics is determined. Then, the fundamental flux per pole Φ_{p1} in the airgap is obtained. Also, its position angle δ_v with respect to q rotor axis is found. This is, in fact, the standard power (voltage) angle. The airgap fluxes in axes d and q are thus,

$$\Psi_{dm} \approx \Phi_{p1} \cdot W_1 \cdot \cos \delta_v \quad (6.12)$$

$$\Psi_{qm} \approx \Phi_{p1} \cdot W_1 \cdot \sin \delta_v \quad (6.13)$$

But, the d - q components of current $\underline{I}_1$ are known, as $\underline{I}_d$, δ_i are known:

$$\underline{I}_{d1} = \underline{I}_1 \cdot \sin \delta_i \quad (6.14)$$

$$\underline{I}_{q1} = \underline{I}_1 \cdot \cos \delta_i \quad (6.15)$$

Consequently, the magnetization reactances as affected by magnetic saturation are calculated:

$$X_{dm} = \frac{\Psi_{dm}}{I_d + I'_f} \quad (6.16)$$

$$X_{qm} = \frac{\Psi_{qm}}{I_q} \quad (6.17)$$

The field current, reduced to the stator, I'_f , was defined earlier in this chapter as a function of actual field current, with its value also given. The stator leakage reactance X_{sl} is considered to be known.

The given phase current I_1 is based on the existence of a DC current I_{dc} (Equation 6.8): $I_{dc} = K_i \cdot I_1 \approx 1.35 I_1$. For given power and neglected diode rectifier losses, given load means given voltage (or load resistance) and current: V_{dc} and I_{dc} . This way, from Equation 6.8, the phase voltage fundamental (RMS) value V_{10} is, in fact, computed. The speed n is known, and thus, $\omega_1 = 2\pi p_1 n$ is given.

With the values of Ψ_{dm} and Ψ_{qm} from FEM, the phasor diagram “produces” the new phase voltage V'_1 :

$$V'_1 = \sqrt{(\Psi_{dm} \omega_1 + X_{l1} I_d + R_1 I_q)^2 + (\Psi_{qm} \omega_1 + X_{l1} I_q - R_1 I_d)^2} \quad (6.18)$$

After the first computation cycle $V'_1 \neq V_{10}$, the computation cycle should restart, with a new value of the current power angle δ_p , but with the same I_1 :

$$\delta_i(K+1) = \delta_i(K) \cdot \left(1 - C_c \cdot \frac{V_1(K) - V_1(K-1)}{V_{10}} \right) \quad (6.19)$$

The coefficient C_c is chosen by trial and error to provide fast convergence, while $V_1(K)$, $V_1(K-1)$ are the calculated phase voltage fundamentals in the K and $K-1$ computation cycles, respectively. If, after a certain number of iterations, convergence is not met, it means that the value of I_1 has to be reduced.

This way, families of saturation curves $\Psi_{dm}(i_{d1} + i'_f, i_{q1})$, $\Psi_{qm}(i_{q1} + i'_f, i_{d1})$ may be obtained to be used later for curve fitting by analytical approximations, for a complete modeling of the machine (as in Chapter 4, *Synchronous Generators*).

Typical I_{dc} vs. speed curves for given battery voltage V_{dc} , obtained through procedures as above, are shown in Figure 6.12 for three different Lundell automotive generators of increasing power.

At the idle engine speed, the current that may be injected in the load, with battery backup, is limited and may constitute a design value in per unit (P.U). Above 3000 rpm, almost full current is produced.

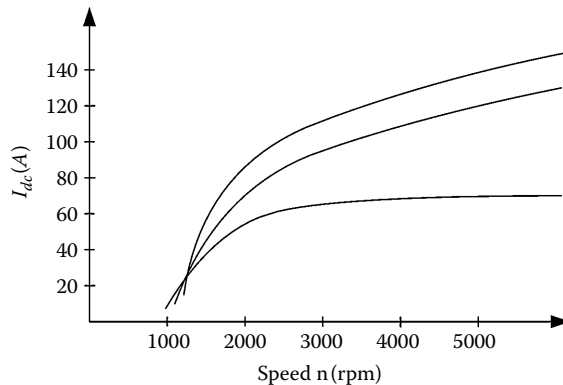


FIGURE 6.12 Typical direct current (DC) output vs. speed for constant battery voltage.

The close to unity power factor in the generator with diode rectifier limits the maximum power (current) that the machine is capable of, for given DC voltage and speed, but the diode rectifier is safe and inexpensive.

To capture the third harmonic in the phase voltages V_{3o} , airgap flux density distribution for final current angle δ_i (and I_1) is decomposed in harmonics, and the third harmonic is detected as amplitude and phase B_{g3} , ϵ_3 , with respect to the fundamental. So, the third harmonic total emf V_{3o} (RMS value) is as follows:

$$V_{3o} = 3 \cdot \Phi_{p3} \cdot \omega_1 \cdot \frac{1}{\sqrt{2}} \cdot W_1; \quad \Phi_{p3} = \frac{2}{\pi} \cdot \frac{\tau}{3} \cdot l_{stack} \cdot B_{g3} \quad (6.20)$$

The phase lag between V_1 and V_{3o} is again ϵ_3 , approximately.

This way, the total phase waveform is obtained as

$$V_a(t) = V_1 \sqrt{2} \cos \omega_1 t + V_{3o} \sqrt{2} \cos(3\omega_1 t - \epsilon_3) \quad (6.21)$$

A comparison of $V_a(t)$ calculated and from tests showed an acceptable agreement [9].

An alternative way to evidenciate the voltage time harmonics consists of using indirectly the FEM to calculate the machine characteristics via an inverse P.U. airgap function $g/g(\beta)$ (obtained from FEM) to define the phase airgap self-inductance and mutual inductance. Unfortunately, this function should account for saturation, which depends on speed, current I_1 , its power angle δ_p , and I_f [6]:

$$\begin{aligned} \left(\frac{g_0}{g} \right) &= [K_1 + K_2 (\cos \beta - 2\omega_1 t + \alpha_2) + K_4 \cos(4\beta - 4\omega_1 t + \alpha_4)] (1 + \cos 6\beta) \\ L_g &= \frac{4\mu_0 \cdot W_1^2 \cdot l_{stack} \cdot \tau}{\pi^2 g_0}; \quad L_{abg} = -\frac{L_{aag}}{3} \\ L_2 &= L_g \cdot \frac{\sqrt{3}}{\pi} \cdot K_2; \quad L_4 = L_g \cdot \frac{\sqrt{3}}{\pi} \cdot K_4 \\ L_{ag}(t) &= L_g \cdot K_1 + L_2 \cos(2\omega_1 t + \alpha_2) + L_4 \cos(4\omega_1 t + \alpha_4) \\ L_{abg}(t) &= -\frac{L_g}{3} K_1 + L_2 \cos\left(2\omega_1 t + \frac{\pi}{3} + \alpha_2\right) + L_4 \cos\left(4\omega_1 t - \frac{\pi}{3} + \alpha_4\right) \end{aligned} \quad (6.22)$$

The coefficients K_1 ($K_1 < 1$), K_2 , K_4 account for magnetic saturation and slot opening effects. And, they depend heavily on speed and load conditions.

Defining this way, phase a self-inductance $L_{aa}(t) = L_{l1} + L_{ag}(t)$; a, b phase mutual inductance $L_{ab}(t) = L_{abg}(t)$; and, in a similar way, the other inductances for sinusoidal current, the steady-state (constant speed and load resistance) performance may be calculated without the d - q model. That is, in phase coordinates,

$$\left| i_{a,b,c,f} \right| \cdot \left| R_1 \right| + \left| V_{a,b,c} \right| = -\frac{d \left| \Psi_{a,b,c} \right|}{dt}; \quad \left| \Psi_{a,b,c} \right| = \left| L_{a,b,c,f}(t) \right| \cdot \begin{bmatrix} i_a \\ i_b \\ i_c \\ i'_f \end{bmatrix} \quad (6.23)$$

Typical results obtained with this method are shown in Figure 6.13 [7].

The highly distorted phase voltage and sinusoidal current waveforms are self-evident.

Note that the presence of booster diodes (the fourth lag of diodes connected to the machine null point) was not yet considered for star connection. So, for most speeds, the phase current is quasi-sinusoidal. For a comprehensive study of a stator excited Lundell generator by FEM, see Reference [8].

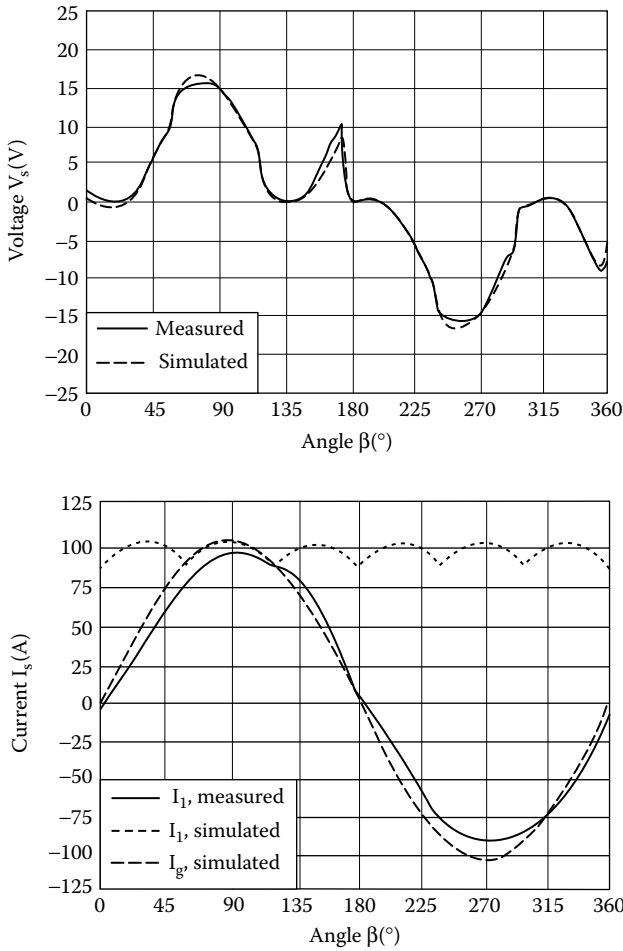


FIGURE 6.13 Phase voltage and current typical waveforms.

6.5 Losses, Efficiency, and Power Factor

Losses occur in the Lundell machine both in the stator and in the rotor:

- Stator iron losses: p_{is}
- Stator copper losses: p_{Cos}
- Rotor copper losses: p_{Cor}
- Rotor claw harmonics losses: p_{claw}
- Mechanical losses: p_{mec}
- Diode rectifier losses: p_{diode}

A complete study of these loss components is presented in Reference [9] with ample experimental results on industrial Lundell generators.

Noticing that, for the star connection of phases, the phase current is quasi-sinusoidal, except for speeds close to ICE idling speed (700 to 1100 rpm), the winding loss calculation is straightforward:

$$p_{Cos} = 3R_l \cdot I_l^2 \quad (6.24)$$

As the field current has small ripple at six times the stator frequency (speed), we may calculate the DC excitation (rotor copper) losses easily:

$$p_{Cor} = R_f \cdot (I_f)_{average}^2 \quad (6.25)$$

Mechanical losses may be approximated by analytical expressions, but measurements are mandatory for a good approximation of them.

The diode rectifier losses, neglected so far, may not be left out in efficiency calculations, as for 14 V_{dc} battery systems they are about as large as the stator copper losses.

$$p_{diode} = 3(\Delta V_{diode} + R_{diode} I_1) I_1 \quad (6.26)$$

where

ΔV_{diode} is the constant voltage drop along a diode

R_{diode} is the equivalent diode resistance

If the diode rectifier loss and the actual power factor $\cos \phi_1$ (close to unity but not equal to) in the machine are considered, the power balance between the machine and the diode rectifier is altered to the following:

$$\begin{aligned} V_1 &= \frac{V_{dc} I_{dc} + p_{diode}}{3 I_1 \cos \phi_1}; \quad K_v = \frac{V_{dc}}{V_1} \\ K_i &= \frac{I_{dc}}{I_1} = \frac{3\sqrt{3}}{\pi} \cdot \cos \phi_1 = 1.35 \cdot \cos \phi_1 \end{aligned} \quad (6.27)$$

For better precision, Equation 6.27 should replace Equation 6.9. In this latter case, K_i varies a little and so does K_v , with speed ($\cos \phi_1$) and current.

The power factor may be attempted based on the idea that it is related to the reactive power Q_{con1} “absorbed” by the commutation inductances of the machine. For sinusoidal current, Q_{con1} is

$$Q_{con1} = 3 \cdot \omega_1 \cdot L_c \cdot I_1^2 \quad (6.28)$$

L_c is, in fact, an average of subtransient inductances of the generator at the d and q axes:

$$L_c \approx \frac{L_d'' + L_q''}{2} < L_d, L_q \quad (6.29)$$

The solid-iron claw poles represent a not so strong but still important damper cage on the rotor. The computation of L_d'' , L_q'' is complicated, and measurements are preferable if comprehensive 3D analytical or numerical (FEM) eddy current models are not used. The power factor is, approximately,

$$\cos \phi_1 \approx \sqrt{1 - \left(\frac{Q_{con1}}{3 \cdot V_1 \cdot I_1} \right)^2} = \sqrt{1 - \left(\frac{\omega \cdot L_c \cdot I_1}{V_1} \right)^2} \quad (6.30)$$

The smaller L_c is, the better.

The power factor decreases slightly with speed (ω_1) for the same V_1 and I_1 , as proven by tests (see the literature [6, 9]).

The claw-pole eddy current losses p_{claw} are produced by the airgap field space harmonics, for sinusoidal stator currents. Claw eddy current losses occur during both no-load and on-load. With no-load, the stator slot openings modulate the excitation-only produced airgap field, and its pulsations are “seen” by the claws as flux density variations. The frequency of these eddy currents corresponds basically to the first slot harmonic and, for $q = 1$, it leads to $6\omega_1$ frequency.

For load conditions, the stator mmf produces space harmonic fields in the airgap, and the fifth and seventh orders are the most important (for $q = 1$). The slot openings appear to augment these harmonics and produce claw-pole eddy currents, as for the no-load situation. As resultant field on load is burdened with local magnetic saturation, the situation becomes further complicated.

A practical analytical model for claw-pole eddy current losses p_{claw} is still not available. Alternatively, when neglecting the eddy currents reaction field, the 3D FEM or MEC models could produce the flux density in all elements, investigated in successive time decrements. Then both the stator iron loss p_{is} and p_{claw} may be calculated through advanced analytical formulas [10].

In these formulas, the loss coefficient in p_{claw} should correspond to solid iron, while for the stator core, it should refer to laminations.

The rotor claw-pole losses tend to increase with speed up to a point and then level out, while the stator iron losses slowly decrease with speed after a peak. The stator current has an increasing demagnetization effect as speed increases because of the need for unity power factor demanded by the diode rectifier.

A qualitative view of machine losses variation with speed for a given DC voltage battery is shown in Figure 6.14. For a detailed study on losses, see Reference [9].

Mechanical, diode, winding, and claw-pole losses are of the same order magnitudes, above 2000 rpm.

Reducing the claw-pole eddy currents by, eventually, using soft magnetic powder material for the claws would notably improve the high-speed performance and power capability of the Lundell generator, provided the mechanical ruggedness is somehow preserved.

Increasing the DC bus (battery) voltage from 14 V to 42 V is another step forward, in the sense that the diode rectifier loss in the P.U. is decreased notably, because the voltage drop on the diode will count less.

Using thinner laminations would notably decrease the stator iron losses, and smoothing the rotor and stator surfaces would reduce the mechanical losses.

Currently, Lundell generators are designed for low volume and costs at the penalty of down to 50% efficiency at high speed and full power. Bold steps in design are required to keep the existing power/volume while increasing the efficiency at moderate cost increases. Such recent attempts are dealt with in what follows.

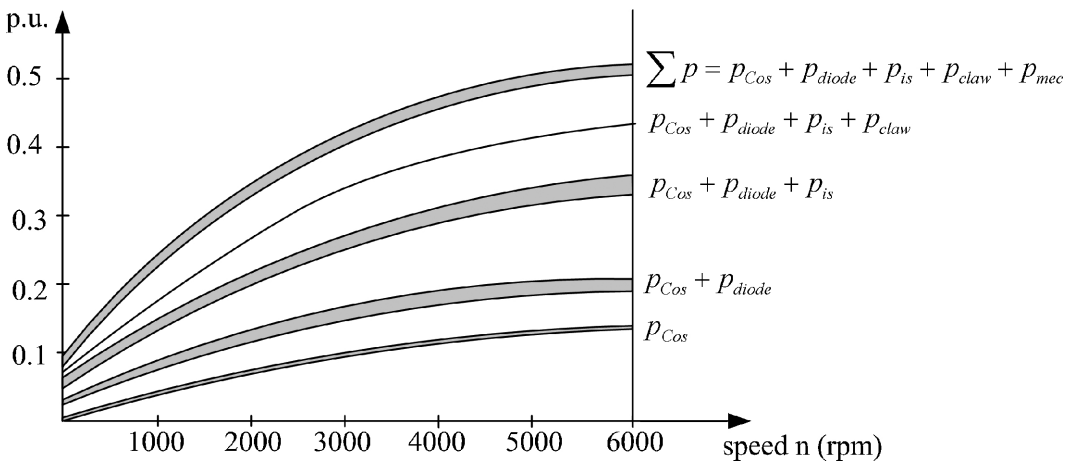


FIGURE 6.14 Loss components vs. speed for a Lundell generator.

6.6 Design Improvement Steps

The modeling tools of the previous paragraph are the foundation for design optimization. A few design improvement steps are discussed:

- Claw-pole geometry optimization
- Booster diodes
- Additional permanent magnets (PMs)
- Increase of the number of poles
- Winding tapping
- Claw-pole damper
- Controlled rectifier
- Winding-type effect on noise level

6.6.1 Claw-Pole Geometry

As shown in Figure 6.6, the claw-pole geometry may be defined by a few parameters, such as claw-pole span α_c (nondimensional), claw-pole angle φ_c , pole pitch τ , claw-pole depth d_c , and the chamfer width W_c .

W_1/τ , W_2/τ — the maximum and minimum claw-pole widths ratios — may be calculated, with α_c , τ , and claw-pole angle φ_c given, as dummy variables.

This small number of independent variables, with all other machine parameters kept constant, makes a direct FEM local optimization feasible through a few tens of runs in practical intervals. Typical results are shown in Figure 6.15 [7].

The penalty function is the DC current, and it is seen that for $\alpha_c \approx 0.45$ and $\varphi_c = 12.5^\circ$, it is maximum.

6.6.2 Booster Diode Effects

A four-legged diode rectifier (Figure 6.16) is sometimes used to tap the third voltage harmonic power. To deliver power through the third harmonic, the latter has to exist both in phase voltage and in current. The connection of the machine null point to diode booster point o leads to nonzero third harmonic phase currents. However, the null current is discontinuous when the third harmonic voltage does not surpass the DC battery voltage, that is, at low speeds.

The presence of the third harmonics power above a certain speed boosts the DC current delivered by the generator for constant battery voltage (Figure 6.17). The effect is notable only at high speeds, where, in general, it is not so much needed. The additional third harmonic current produces stator copper and stator core and claw-pole losses.

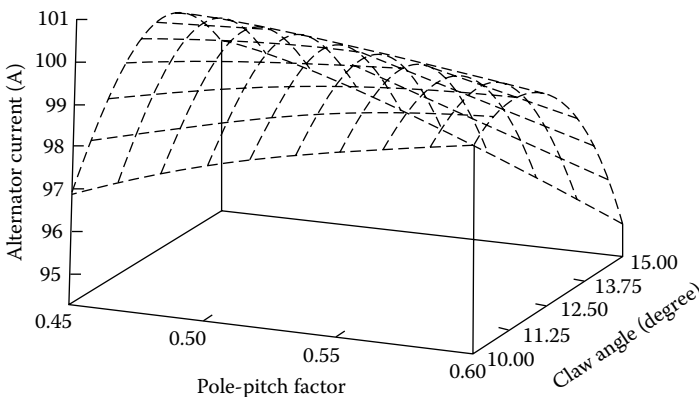


FIGURE 6.15 Generator direct current (DC) vs. pole pitch α_c coefficient and φ_c .

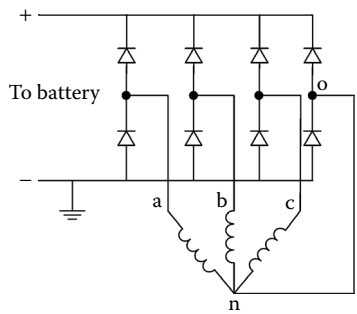


FIGURE 6.16 Diode rectifier with power booster diodes.

At least for constant speed synchronous generators with discontinuous current, at 50 (60) Hz, it was proven that the extra power with booster diodes offsets the increase in losses, and an increase in efficiency of about 1% is obtained [11], together with 10 to 15% more power.

For automotive applications, the advantages of booster diodes are considered to be less practical, as they do not come at low speeds when more power is badly needed.

6.6.3 Assisting Permanent Magnets

Axially magnetized permanent magnets may be added beside the coil (Figure 6.18a), on the surface of the claws (Figure 6.18b), and between the claws (Figure 6.18c). In the third case, the permanent magnets act to destroy the interclaw leakage flux of excitation coil. The first two solutions are meant to produce more excitation flux for lower excitation losses. Unfortunately, at low speeds, the influence on the DC output current is small due to magnetic saturation (Figure 6.19).

Due to high leakage flux, the PMs on the shaft produce little effect at all speeds. The surface PMs produce notably larger DC output current but, unfortunately, only at higher speeds.

The PMs placed between the claws destroy the excitation leakage flux in the rotor and, thus, enhance the main flux for all speeds. The improvement occurs at all speeds (Figure 6.19) [7].

Though the interclaw PMs, eventually made of ferrites, seem to be an attractive solution, their mechanical placement and integrity are not easy to secure.

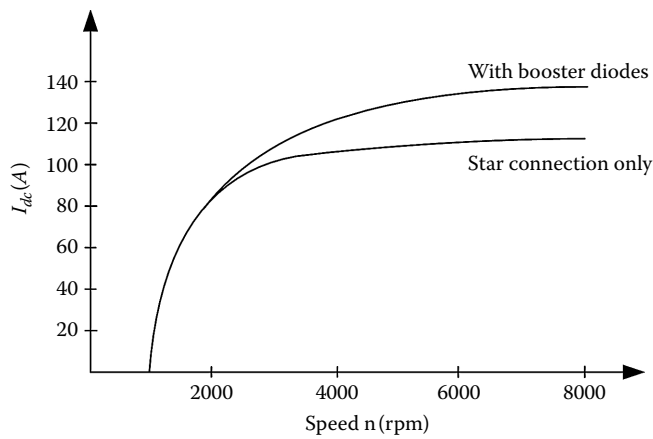


FIGURE 6.17 Booster diode effect on direct current (DC) vs. speed.

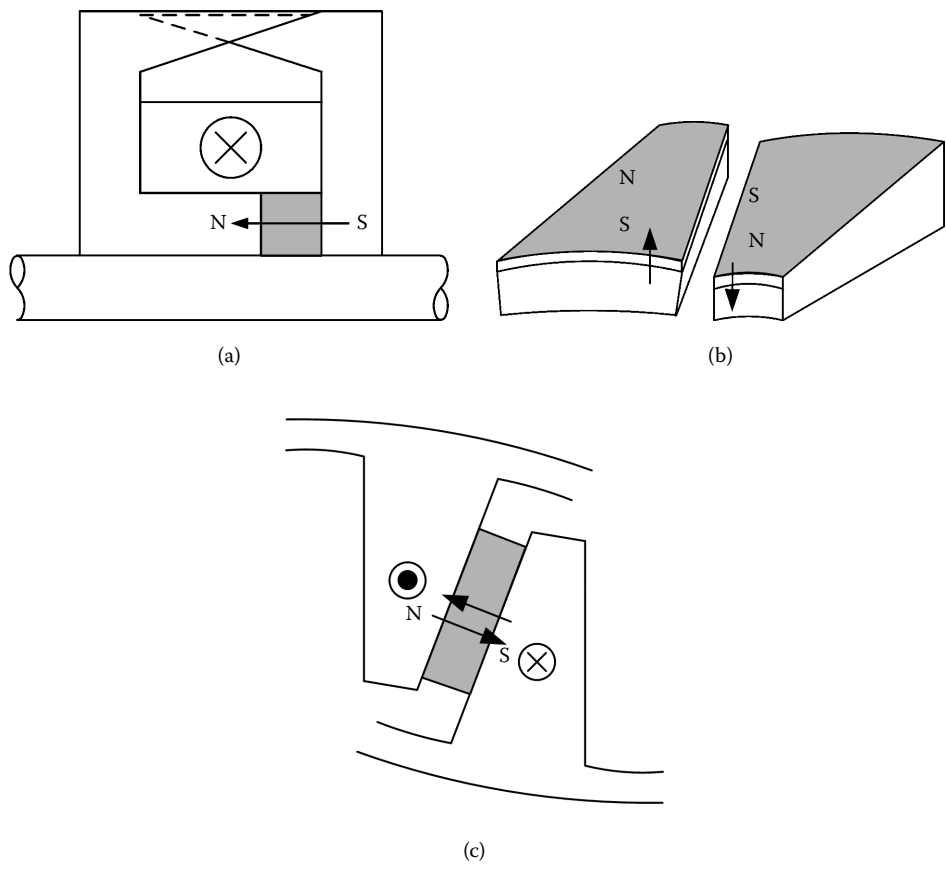


FIGURE 6.18 Placing additional permanent magnets (PMs) for given stator stack: (a) PMs on the shaft, (b) PMs on the claw surface, and (c) PMs between claws.

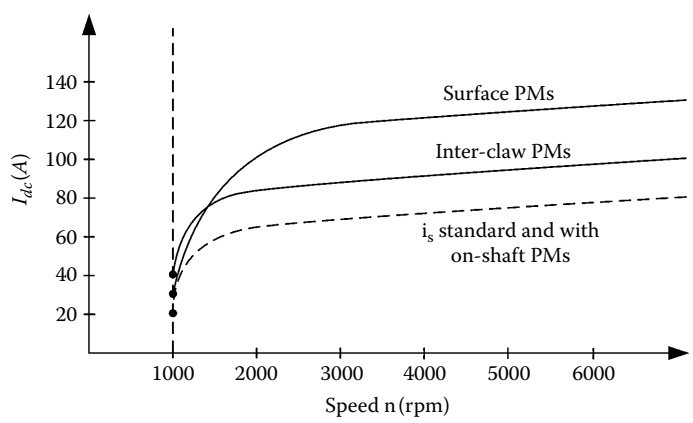


FIGURE 6.19 Influence of additional permanent magnets (PMs) on direct current (DC) output for the same stator.

6.6.4 Increasing the Number of Poles

Increasing the pole number, for given stator and rotor diameters and lengths, and at the same total stator slot area, seems to be another way to notably increase the output. The reason is the torque multiplication effect of increasing the “speed” of flux variation.

When the number of poles goes up from $2p = 12$ (the standard) to 14 and 16, the DC output current steadily increases, especially above 2000 rpm [7].

Unfortunately, the frequency $f_1 = p_1 \cdot n$ also increases with pole number. Consequently, the iron losses tend to increase, especially in the stator teeth. Additionally, the claw eddy current losses tend to increase.

For larger power Lundell generators — used for trucks or buses — such a solution may prove to be better. Still, the effect at low speed is not so important.

6.6.5 Winding Tapping (Reconfiguration)

It is a known fact that at low speeds the Lundell generator is not capable of producing enough DC due to insufficient emf, as the number of turns is kept low to secure a good cross-section of copper in the coils and so as to handle large currents at high speeds. Increasing the number of turns per phase (or per current path) at low speeds while reducing it at high speeds is the way out of this difficulty.

Switching the machine connection from star to delta or tapping the stator winding are two alternatives to produce increased output. By halving the winding tapping reduces twice the stator resistance twice (Figure 6.20).

To produce large output at low (idle engine) speed, the number of turns per phase has to be increased: $W'_1 > W_1$. If the total area of slots remains constant, the $W_1 \cdot I_1$ should be the same for a given current density. However, an increase in W_1 (to W'_1) should be accompanied by a reduction of copper wire cross-section; thus, higher current density at higher (or same) current is required:

$$W_1 \cdot A_{Co} \approx W'_1 \cdot A'_{Co} \tag{6.31}$$

with W_1 , W'_1 , A_{Co} , and A'_{Co} equal to the initial and modified total number of turns and copper wire cross-section, for the tapped winding design. Increasing W_1 and W'_1 when the winding tapping is applied leads to higher maximum emf at idle engine speed. This is essential progress.

Neglecting the machine saliency ($X_d = X_q = X_s$), the phasor diagram in Figure 6.11, at unity power factor, takes shape in Figure 6.21.

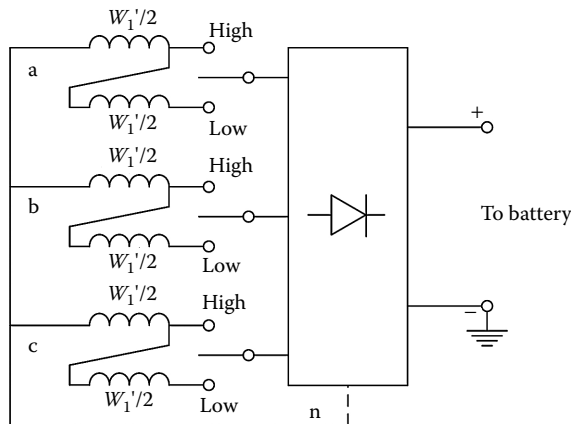


FIGURE 6.20 Winding tapping (halving).

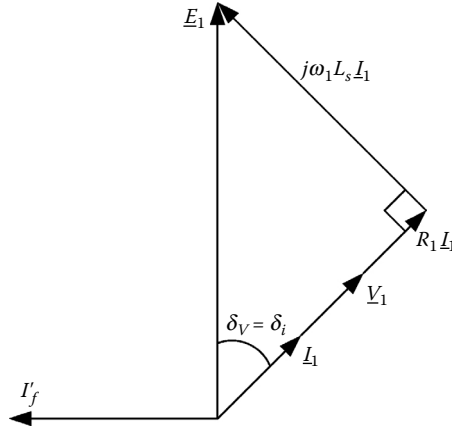


FIGURE 6.21 Simplified phasor diagram ($X_d = X_q$) with diode rectifier ($\cos\phi_1 = 1$).

Only the fundamental components of generator phase voltage and current are considered for simplicity. The stator resistance R_1 and R'_1 (initial and new) are as follows:

$$R_1 = \rho_{Co} \cdot l_c \cdot \frac{W_1}{A_{Co}}; R'_1 = \rho_{Co} \cdot l_c \cdot \frac{W'_1}{A'_{Co}} \quad (6.32)$$

with l_c as coil length.
The reactance X_s is

$$\begin{aligned} X_s &\approx \omega_1 \cdot L_s = \omega_1 \cdot K_L \cdot W_1^2 \\ X'_s &= \omega_1 \cdot K_L \cdot W_1'^2 \end{aligned} \quad (6.33)$$

The emfs are

$$\begin{aligned} E_1 &= K_e \cdot \omega_1 \cdot W_1 \\ E'_1 &= K_e \cdot \omega_1 \cdot W'_1 \end{aligned} \quad (6.34)$$

From the phasor diagram, the following voltage relationship is obtained:

$$E_1 = \sqrt{(\omega_1 \cdot L_s \cdot I_1)^2 + (V_1 + R_1 \cdot I_1)^2} \quad (6.35)$$

$$E'_1 = \sqrt{(\omega_1 \cdot L'_s \cdot I'_1)^2 + (V_1 + R'_1 \cdot I'_1)^2} \quad (6.36)$$

The problem is to yield a higher current I_1 at idle engine speed (ω_{1min}) for the same battery voltage with a larger number of turns, that is, larger L_s and R_s ; $L'_s > L_s$ and $R'_s > R_s$.

For simplicity, let us neglect the $R_1 I_1$ terms to obtain the following:

$$I_1^* \approx \frac{\sqrt{E_1^{*2} - V_1^{*2}}}{\omega_1 \cdot L_s} = \frac{\sqrt{K_e^2 \cdot \omega_1^2 \cdot W_1'^2 - V_1^2}}{\omega_1 \cdot K_e \cdot W_1'^2} \quad (6.37)$$

It is evident from Equation 6.35 that there is an optimum number of turns that produce the maximum current:

$$W_{1opt} = \frac{V_1 \cdot \sqrt{2}}{K_e \cdot \omega_1} \quad (6.38)$$

Expression 6.38 spells out the fact that for maximum current output at given speed, the number of turns should vary inversely proportional to speed so that the emf is $\sqrt{2}$ times the phase voltage ($E_{1opt} = K_e \cdot \omega_1 \cdot W_{1opt} = V_1 \cdot \sqrt{2}$). If the initial design is fixed at W_{1opt} , it should remain so, and nothing is gained at low speeds. At high speeds, the halving of the winding will produce more DC output current for slightly more losses. However, if for the existing design $W_1 < W_{1opt}$, then it should be brought to W_{1opt} while reducing, accordingly, the cross-section of copper wire.

The DC output current at idle engine speed will increase to its maximum value:

$$I_{max} \approx \frac{V_1}{\omega_1 \cdot L_s} \quad (6.39)$$

At high speeds, with winding halving, more current will be obtained but, again, with slightly more copper losses.

6.6.6 Claw-Pole Damper

Still another way to increase the DC output current at all speeds consists of decreasing the commutation inductance L_c of the machine and, thus, reducing the diode commutation process reactive power.

But, the commutation inductance $L_c = (L_d'' + L_q'')/2$ relies on the damper effect of the rotor solid claw poles. An additional damper placed between the claws in the form of solid aluminum plates should do (Figure 6.22). If the electrical contact between the aluminum dampers and the solid iron of claws is good, their combination will act more like a one-piece conductor for the space harmonics field of the stator. A stronger damper winding is obtained.

A practical technology for tightly sticking the aluminum dampers between the claw poles is still to be developed, but decreasing the commutation (subtransient) inductance of the machine seems to be a practical way to increase output.

Increasing the airgap would also help in terms of obtaining more output from the same volume but for higher losses in the excitation winding. Also, if the airgap is increased too much, the interclaw excitation leakage field tends to increase. Preliminary test results presented in Reference [12] seem to substantiate the aluminum damper's advantages.

6.6.7 The Controlled Rectifier

Yet another way to increase the output at low speeds is to use a controlled thyristor rectifier instead of the diode rectifier. This way, the unity power factor condition is eliminated, and reactive power exchange capability with the battery is established. The delivered power is approximately

$$P = \frac{3 \cdot E_1 \cdot V_1 \cdot \sin \delta_v}{X_s} \quad (6.40)$$

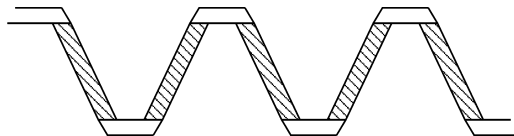


FIGURE 6.22 Aluminum damper pieces.

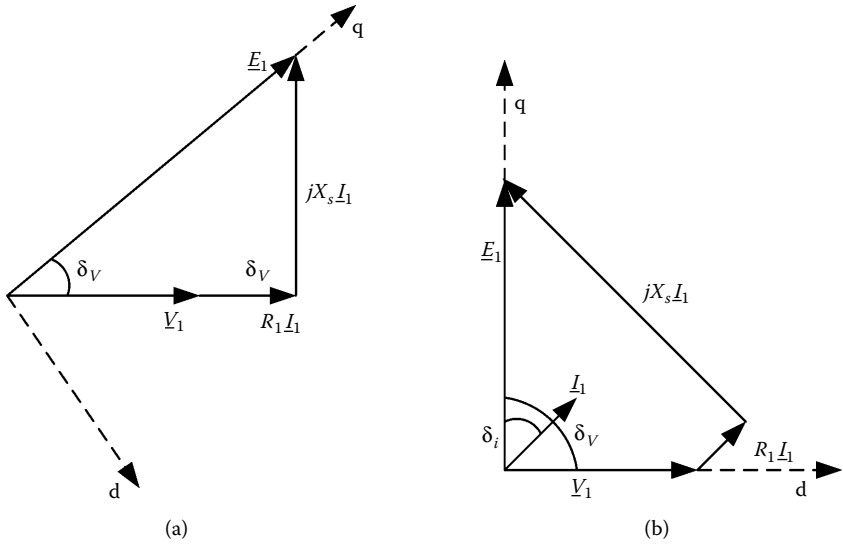


FIGURE 6.23 Simplified phasor diagrams ($X_d = X_q = X_s$): (a) with diode rectifier $\delta_V < 90^\circ$ and (b) with controlled rectifier ($\delta_V = 90^\circ$).

The voltage power angle δ_V of the machine could be raised close to 90° to produce more output [13] (Figure 6.23a and Figure 6.23b).

For the same emf E_1 in Figure 6.23a and Figure 6.23b, more current is feasible at given voltage V_1 and speed (X_s), as the $X_s \cdot I_1$ vector changes the location in the rectangular triangle.

An increase in DC output current of almost 50% is reported in Reference [13].

Furthermore, the introduction of the controlled rectifier may be accompanied by winding reconfiguration (Figure 6.24), when the controlled rectifier works on the full winding at low speed, while a (additional) diode rectifier works at high speed, on half the winding, to combine the advantages of both methods [13].

The switches A and B are closed only for low speed. The speed for opening switch A is a matter of compromise and depends on the winding tap point, which, in general, may be placed at 1/2 or 1/3 P.U. distance from the null point. The tap point should be easily accessible. Reference [13] reports not only improved DC output current at low and especially at high speeds, but also an increase in efficiency.

The added complexity and costs of equipments are the prices to pay for a 5 to 10% increase in generator–rectifier system efficiency (from 47 to 57%).

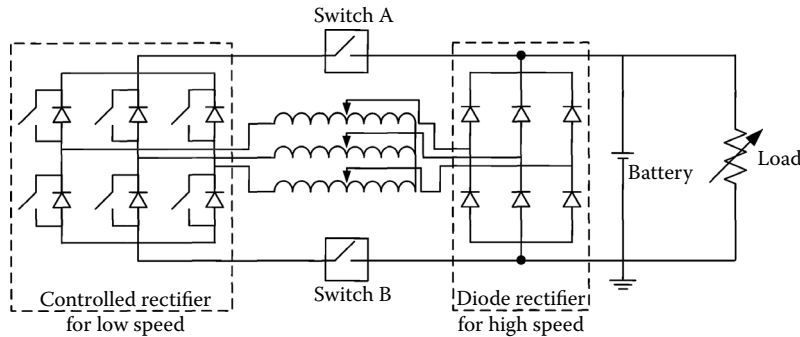


FIGURE 6.24 Winding reconfiguration with controlled rectifier.

A switched diode rectifier may also be used to boost the generator voltage at low speeds and buck it at high speeds, in order to increase the output of an existing claw-pole-rotor alternator.

Note that the design problems of the Lundell alternator do not end here. Designers should continue with noise and vibration and thermal and mechanical redesigns [14].

6.7 The Lundell Starter/Generator for Hybrid Vehicles

Recently, the Lundell machine was used on the first commercial mild hybrid electrical vehicle. Its use as a starter presupposes inverter (frequency) control and, thus, implicitly controlled rectification capability during generating mode. The design accent will be placed on motoring mode with verifications for the generating mode.

The efficiency problem becomes very important, besides size, both during starting assistance of the ICE, and during driving the air-conditioning system when the ICE is shut down in traffic jams. The energy extracted from the battery is decreased, and thus, increases in gas mileage and battery life are obtained (Figure 6.25) [15].

The power requirements of air conditioners increases to a peak 2.5 kW (for a luxury car) at 15,000 rpm, and the peak torque is about constant, up to maximum speed. Through an electric clutch, placed on the ICE shaft, the single, ribbed, belt transmission allows for the starter/generator to engage the ICE when the latter is on and, respectively, the air conditioner, power steering, and water pumps (auxiliaries) otherwise. The conventional starter is still there for first (morning) start.

Mild hybrid vehicles rely on the contribution of the starter/generator supplied from the 42 V_{dc} battery. Three 14 V_{dc} batteries in series, with a typical 100 A maximum current absorption (for a number of cycles corresponding to 5 yr of regular driving), provide for a moderate cost battery. The battery also

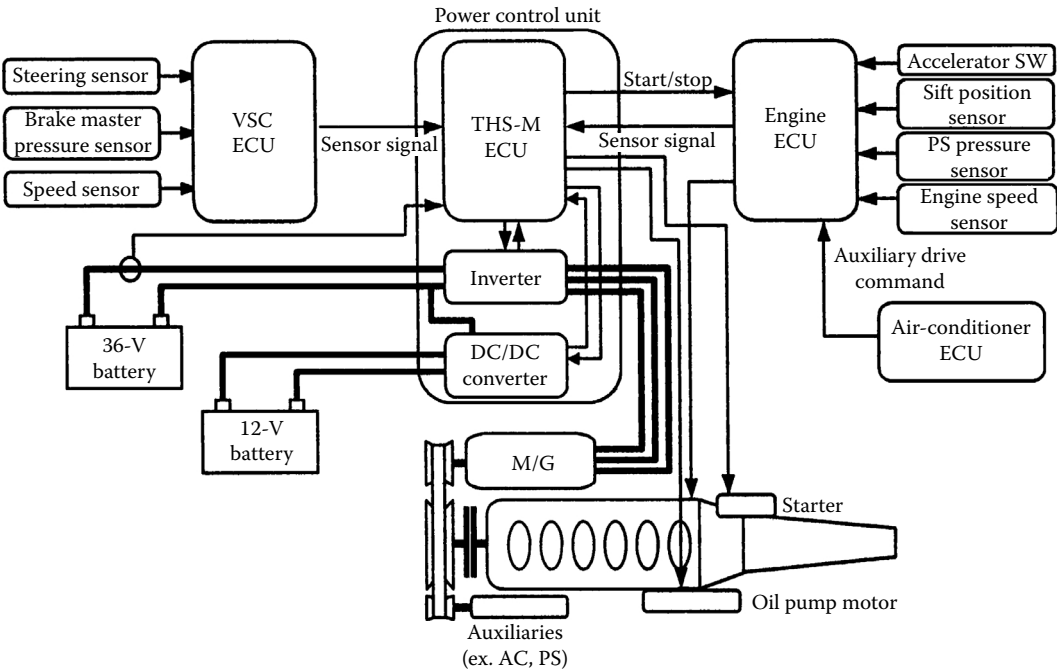


FIGURE 6.25 Mild hybrid vehicle system configuration. (Adapted from T. Teratani, K. Kurarnoki, H. Nakao, T. Tachibana, K. Yagi, and S. Abae, Development of Toyota mild hybrid systems (THS-M) with 42 V power net, Records of IEEE-IEMDC-2003, Madison, WI, 2003.)

serves all the auxiliary equipment on board a medium to large car. A 14 V_{dc} bus is built through a special DC–DC converter to serve the low voltage loads.

The starter–alternator design should be worked on with the following objectives in mind:

- Maximum motoring torque for starting and assisting the ICE at low speed, and for driving the ICE air conditioner compressor load when the ICE is off
- Maximum generating power vs. speed
- Observation of the battery state and reduction of the losses in the system to obtain a reasonable battery life
- Minimum possible machine volume and pulse-width modulator (PWM) converter costs

As a result, the main specifications for the Lundell starter–generator in Reference [15] are as follows:

- Rated voltage: 36 V (42 V_{dc} bus)
- Rated output: motoring, 3 kW; generating, 3.5 kW
- Maximum torque: 56 Nm at 300 rpm
- Permissible maximum speed: 15,000 rpm
- Air cooling

Typical battery specifications are as follows:

- Capacity: 20 Ah valve-regulated lead acid battery
- Weight: 27 kg
- Volume: 9.21 l
- Starting performance: 6.1 kW for 1 sec
- Auxiliary drive: 2.1 kW
- Regenerative performance: 3.5 kW for 5 sec

There are three ways to save fuel with the starter–generator system:

- Starting assistance of ICE
- Energy saving during “idle stop,” when the ICE is shut down at traffic lights or in traffic jams
- Regenerative electric braking of the vehicle as much as the battery recharging permits

A sophisticated control system is needed to perform these actions, and a 40% fuel savings in town driving and 15% for overall driving was reported with such a system that now costs probably about \$1500 and fills within the volumes of an existing car.

Here we will pay attention to Lundell starter–generator control through the PWM inverter, as coordinated excitation and armature control are required for optimum performance in the four main operation modes:

- Vehicle stopping (idle stop): the Lundell machine drives the air conditioner, power steering, and other auxiliaries
- Starting: after initial starting by the starter, the Lundell machine restarts the ICE
- Normal driving: generating as needed by the battery monitored state
- Deceleration: regenerative braking to recharge the battery

An intelligent power metal-oxide semiconductor field-effect transistor (MOSFET) module PWM inverter is used.

The Lundell machine may be either vector or direct torque and flux controlled (DTFC) [16, 17]. In what follows, we will dwell on DTFC, as it leads to an inherently more robust control. The rotor position is required for speed calculation and in the flux observers but not for coordinate transformation in the control. Also, DTFC may be adapted easily for the generating operation mode.

Essentially, for motoring, the Lundell machine should be controlled at unity power factor to minimize the machine losses and inverter kilovoltamperes. The generator excitation-only control may be used with the MOSFETs inhibited in the converter, where only the diodes are working.

The torque remains the main controlled variable during motoring — for starting the ICE operation mode. For auxiliary motoring (idle stop operation mode), speed control may be used by adding an external speed control loop.

As at low speed, calculations of the power factor are not very robust, a feed-forward field current calculator is added. The stator flux reference level is adapted to speed for generating and motoring (during idle stop).

The d - q model of the Lundell machine is imperative in conceiving the DTFC system for the former. The d - q model of the SM is as follows [16]:

$$\begin{aligned}
 \bar{i}_s R_s - \bar{V}_s &= -\frac{\partial \bar{\Psi}_s}{\partial t} + j\omega_r \bar{\Psi}_s \\
 \bar{i}_s &= i_d + ji_q; \quad \bar{V}_s = V_d + jV_q; \quad \bar{\Psi}_s = \Psi_d + j\Psi_q \\
 \Psi_d &= L_{sl} i_d + \Psi_{dm}; \quad \Psi_{dm} = L_{dm} i_{dm}; \quad i_{dm} = i_d + i_{dr} + i'_f \\
 \Psi_q &= L_{sl} i_q + \Psi_{qm}; \quad \Psi_{qm} = L_{qm} i_{qm}; \quad i_{qm} = i_q + i_{qr} \\
 i'_f R'_f - V_f &= -\frac{\partial \Psi'_f}{\partial t}; \quad \Psi'_f = L'_{fl} i'_f + \Psi_{dm}; \quad R_s = R_l - \text{phase resistance} \\
 i_{dr} R_{dr} &= -\frac{\partial \Psi_{dr}}{\partial t}; \quad \Psi_{dr} = L_{sl} i_{dr} + \Psi_{dm} \\
 i_{qr} R_{qr} &= -\frac{\partial \Psi_{qr}}{\partial t}; \quad \Psi_{qr} = L_{sl} i_{qr} + \Psi_{qm} \\
 T_e &= \frac{3}{2} p_1 \text{Real}(j\Psi_s i_s^*) = \frac{3}{2} p_1 (\Psi_d i_q - \Psi_q i_d)
 \end{aligned} \tag{6.41}$$

i_{dr} and i_{qr} are the rotor damper (claw) currents reduced to the stator.

For steady state, in rotor coordinates, $\frac{\partial}{\partial t} = 0$ and, for unity power factor, the space-lag angle between the stator voltage and current vectors (V_s, I_s) is zero, and thus, the stator flux vector is Ψ_s and is located at 90° (electrical) with respect to V_s :

$$\bar{i}_{so} R_s - \bar{V}_{so} = -j\omega_r \bar{\Psi}_{so} \tag{6.42}$$

For $\cos\phi_1 = 1$,

$$\begin{aligned}
 i_{so} R_s - V_{so} &= -\omega_r \Psi_{so}; \quad \Psi_{so} = L_{sl} \cdot i_{do} + L_{dm} (i_{do} + i'_{fo}) + j(L_{sl} + L_{qm}) \cdot i_{qo} \\
 T_e &= \frac{3}{2} p_1 \cdot \Psi_{so} \cdot i_{so} \\
 i'_{fo} &= \frac{V'_f}{R'_f}, \quad i_{dro} = i_{qro} = 0
 \end{aligned} \tag{6.43}$$

The vector diagram of the Lundell machine for $\cos\phi_1 = 1$ (and steady state) is shown in Figure 6.26. It resembles the phasor diagrams dealt with earlier in this chapter, but in the time delay angle, there are now space angles of the same value, and one vector represents the whole machine and not only a phase.

The vector diagram allows for computation of field current i_{fo}^* for given stator flux Ψ_{so}^* and torque T_{eo}^* . From the torque expression (Equation 6.43), the stator current i_{so}^* is as follows:

$$i_{so}^* = \frac{2}{3} \cdot \frac{T_{eo}^*}{p_1 \cdot \Psi_{so}^*} \tag{6.44}$$

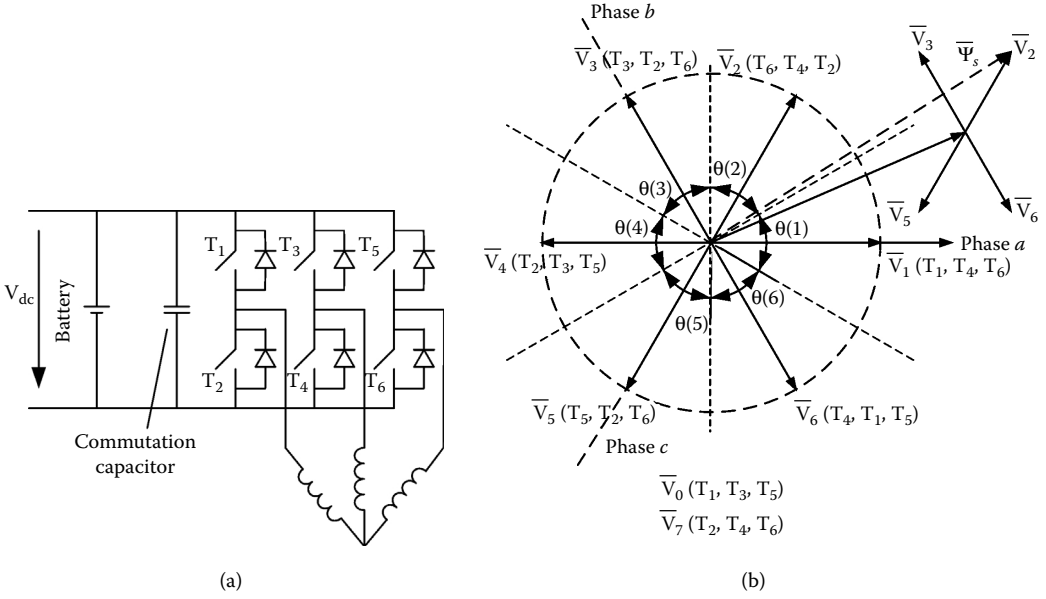


FIGURE 6.27 (a) Typical voltage-source pulse-width modulator (PWM) inverter and (b) its voltage vectors.

The vector definition, in stator coordinates, is

$$\bar{V}_s = \frac{2}{3} \left(v_a + v_b e^{j\frac{2\pi}{3}} + v_c e^{-j\frac{2\pi}{3}} \right) \quad (6.49)$$

For $\bar{V}_1$: $v_a - v_b = V_{dc}$, $v_b = v_c$ and $v_a + v_b + v_c = 0$, and thus, $v_a = \frac{2}{3}V_{dc}$, $v_b = v_c = -\frac{1}{3}V_{dc}$. For the other voltage vectors, similar expressions are obtained.

In DTFC, the stator flux $\bar{\Psi}_s(\Psi_s, \delta_\Psi)$ and the torque T_e are estimated and compared to their desired values Ψ_s^* and T_e^* to find the errors $\mathcal{E}_{\Psi_s}$ and $\mathcal{E}_{T_e}$:

$$\begin{aligned} \mathcal{E}_{\Psi_s} &= \Psi_s^* - \Psi_s \\ \mathcal{E}_{T_e} &= T_e^* - T_e \end{aligned} \quad (6.50)$$

Together with the stator flux, δ_Ψ , $\mathcal{E}_{\Psi_s}$, and $\mathcal{E}_{T_e}$ (values and sign) form the variables by which a single voltage vector (or a given sequence of them) is triggered in the converter. No coordinate transformation is needed for control.

Though the stator flux vector position angle δ_Ψ may be precisely found, only its location in one of the six (60° wide) sectors in Figure 6.27b is needed.

The method for choosing the adequate voltage vector for given stator flux vector $\theta(i)$ and torque and flux errors comes from the stator equation in stator coordinates:

$$\bar{i}_{ss} R_s - \bar{V}_{ss} = -\frac{d\bar{\Psi}_{ss}}{dt} \quad (6.51)$$

where, if $R_s \approx 0$,

$$\bar{\Psi}_{ss} - \bar{\Psi}_{ss}^0 \approx \int_0^T \bar{V}_{ss} dt = \bar{V}(i) \cdot T \quad (6.52)$$

The flux vector variation falls along the applied voltage vector direction.

TABLE 6.1 Direct Torque and Flux Control (DTFC)
Switching Table

$\theta(i)$							
ε_{Ψ_s}	ε_{T_e}	$\theta(1)$	$\theta(2)$	$\theta(3)$	$\theta(4)$	$\theta(5)$	$\theta(6)$
1	1	V_2	V_3	V_4	V_5	V_6	V_1
1	-1	V_6	V_1	V_2	V_3	V_4	V_5
0	1	V_0	V_7	V_0	V_7	V_0	V_7
0	-1	V_0	V_7	V_0	V_7	V_0	V_7
-1	1	V_3	V_4	V_5	V_6	V_1	V_2
-1	-1	V_5	V_6	V_1	V_2	V_3	V_4

Also, for increasing motoring torque, the flux vector has to be accelerated in the direction of motion. It should be decelerated for motoring torque reduction.

For the flux vector in Section 1 ($\theta(1)$), to increase the flux amplitude and increase the torque, $\overline{V_2}$ should be turned on; to increase the torque, but decrease the flux, $\overline{V_3}$ is chosen; to decrease the torque and increase flux, $\overline{V_6}$ is needed; while to decrease torque and decrease flux, $\overline{V_5}$ is applied. A unique table of commutations is thus feasible (Table 6.1) (Reference [16], p. 232).

The flux and torque error value of 1 means positive value, while -1 means negative value. To improve the stator current waveform and reduce torque pulsations, instead of each voltage vector in Table 6.1, a combination of the two neighboring vectors and a zero vector, or a more sophisticated regular combination, may be implemented. This is space-vector modulation (SVM) in a kind of random “expression.”

Though the 1, -1, 0 outputs of flux and torque errors suggest hysteresis torque and flux regulators, more advanced (proportion integral [PI], sliding mode) regulators were recently used with remarkable success [18].

The general control scheme for motoring is as in Figure 6.28. In general, full flux is needed for full torque at low speeds. The eventual errors in the machine parameters, as influences on i_f^* , are corrected by the power factor regulator.

A four-quadrant DC-DC converter seems best to control the field current in order to obtain very fast field current response. This way, overvoltages are avoided, and the torque response is faster. The stator flux and torque estimator configurations are a matter of choice from many possibilities [16]. Here, a combined voltage-current model is used, as position feedback θ_{er} is available (Figure 6.29a and Figure 6.29b).

The PI compensator drives to zero the error between the voltage and current models, providing for current model domination at low frequencies (speeds). In general, only the DC voltage is measured, but for the 42 V_{dc} system, care must be exercised at very low speeds to correct for the stator resistance variation and for MOSFET voltage drops in the inverter. The AC voltages are constructed from the knowledge of the active voltage in the inverter and the DC voltage, as discussed earlier in this paragraph.

The errors in parameters for current model have an effect only at low speed, where the current model is dominant. The trustworthy encoder feedback should secure fast and reliable torque response of the system from zero speed. A robust position and speed observer from zero speed is another possibility. The reference torque T_e^* should be determined in a torque sharing program based on speed, total drivetrain torque requirements, battery stage, comfort, and so forth. A speed close loop may be added to provide reference torque of the air conditioner during idle stop driving.

For the generating mode, it is possible to use full control of the converter as for motoring but with the reference flux tailored with speed:

$$\Psi_s^* \approx \frac{V_{1\max}}{\omega_r} (1.03-1.05) < \Psi_{s\max}^* \quad (6.53)$$

The torque reference T_e^* should now be negative and may be calculated based on braking paddle position during braking, with a limitation based on the battery energy acceptability.

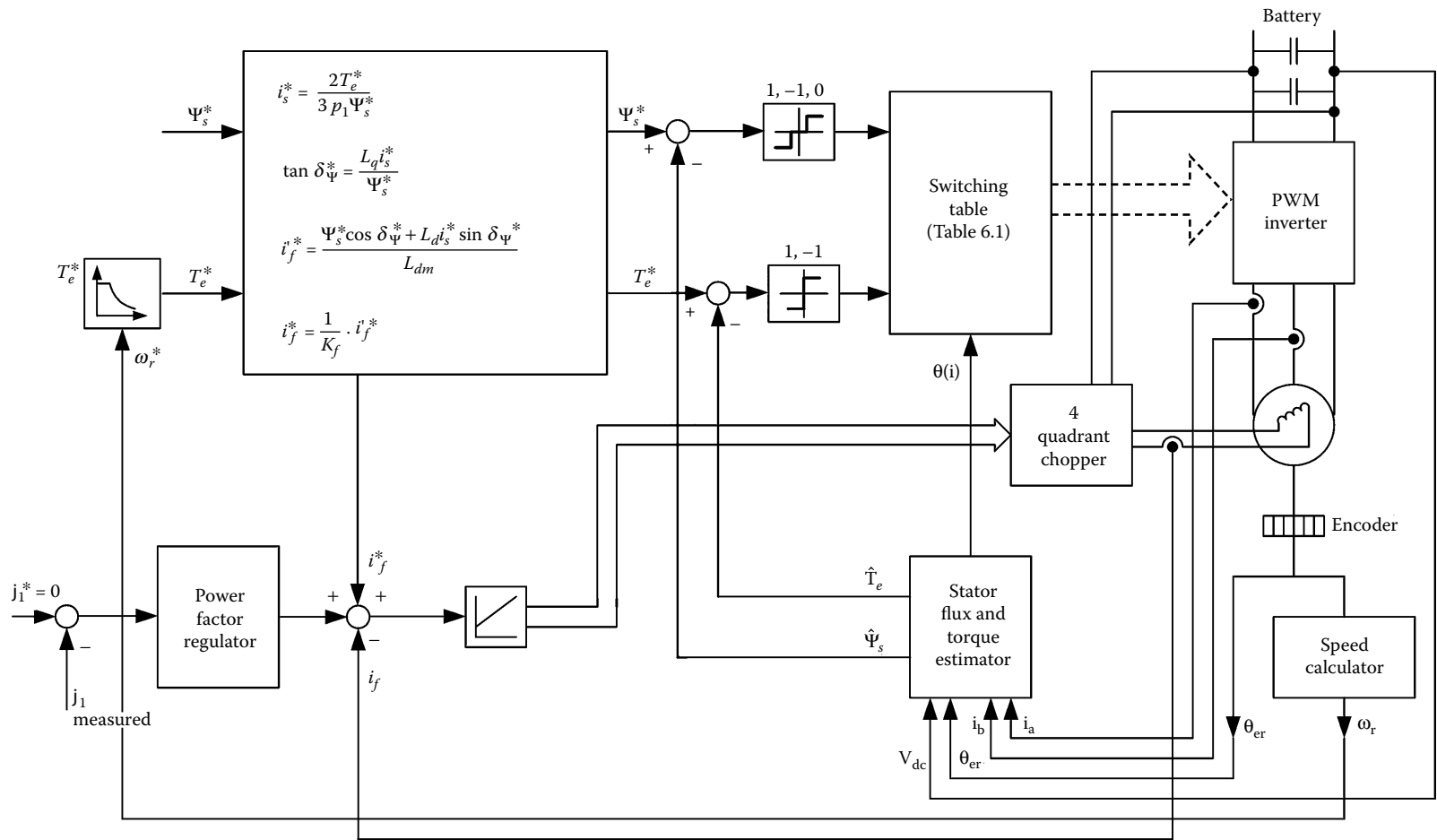


FIGURE 6.28 Motoring direct torque and flux control (DTFC) of Lundell machine.

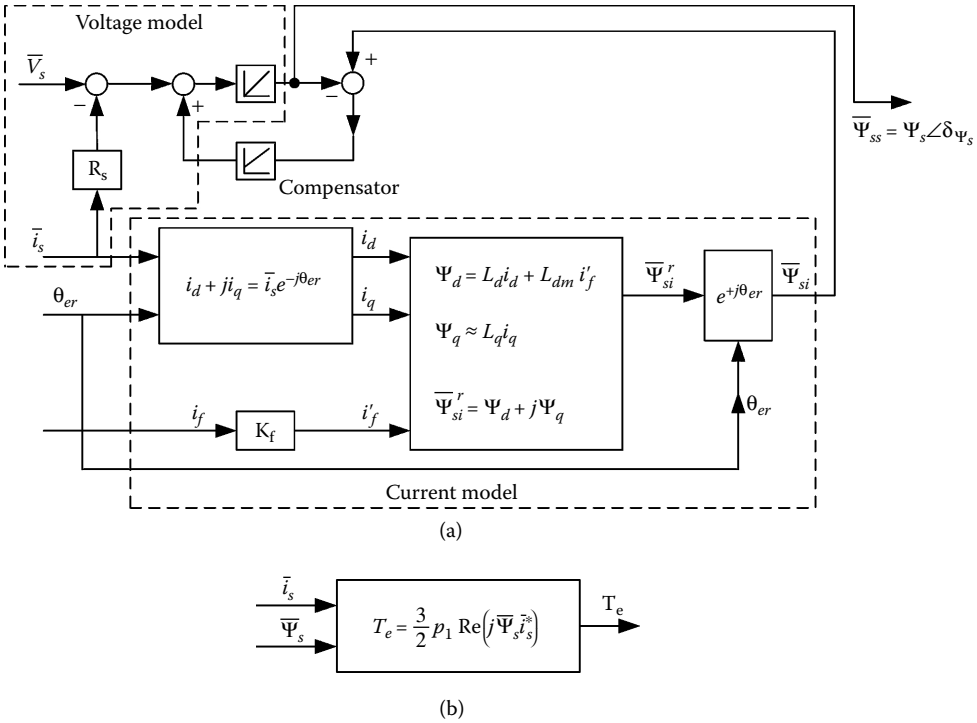


FIGURE 6.29 Typical (a) flux observer and (b) torque calculator.

During normal driving, based on battery voltage and its state of charge and load requirements, a certain power P_e^* is required. A power close loop may be added to generate the negative torque reference after division by speed.

These two options are depicted in Figure 6.30.

It is also possible to totally inhibit the MOSFET in the inverter during generating modes and let the diodes do the rectification, with field current control only. In this latter case, the torque reference (in Figure 6.30) should act directly (after scaling) as excitation current reference i_f^* (Figure 6.28).

As out-of-town driving may be predominant, generating is required a lot, and standard generating control (by field current) might be the way to obtain greater reliability of the control system.

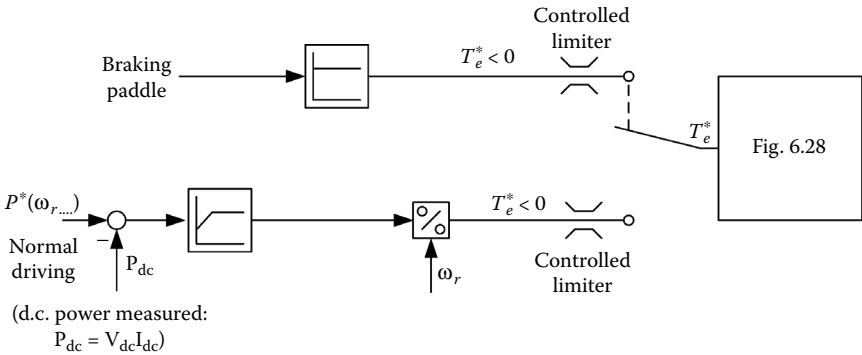


FIGURE 6.30 Generator control settings.

Note that the rather low efficiency in existing Lundell standard generator designs has to be improved notably for the starter-generator in mild hybrid cars. Allowing for more volume and using more copper in the windings, which then have to be manufactured and inserted in a special manner, seems to be the evident way to better performance.

The use of the Lundell machine in mild hybrid vehicles is motivated by the fact that it is a proven technology, and it has very good generating characteristics over a wide speed range, which are necessary characteristics in the application. The exclusive field current control during generating is another essential advantage of the claw-pole-rotor starter-alternator.

6.8 Summary

- Electric power needs on board ICE vehicles (cars, trucks, buses) is on the rise.
- The Lundell machine with single ring-shape coil multipolar rotor controlled excitation and diode rectifier output is the only generator technology on board road vehicles today. It is basically a synchronous generator.
- The power and volume of the Lundell machine is becoming a problem with power per unit on the rise. The low efficiency of the system (about 50%) is already a big issue, as it hampers the gas mileage.
- The improvement of Lundell machine performance (efficiency and power and volume) for reasonable extra costs per kilowatt of installed power is the key design issue for securing some future for this generator in automotive applications.
- The power and volume restrictions led to a high number of poles (12 in general).
- To reduce DC excitation losses for a 12-pole excitation, the homopolar rotor configuration leads to a very good solution.
- The same large number of poles and winding manufacturing cost limitations prompted the use of a single-layer three-phase winding on the stator with three slots per pole ($q = 1$). Only in large power units $q = 2$ (six slots per pole).
- The three slots per pole choice leads to important low-order space harmonics in the airgap permeance with a dominant emf third harmonic that increases with speed but only up to 10%.
- During on-load operation, the three slots per pole, slot openings, and claw-pole geometry concur to produce a much stronger third harmonic in the airgap (resultant) emf, especially at high speeds for star connection of phases.
- For star connection of phases, which is the dominant solution, the diode rectifier leads to discontinuous (trapezoidal shape) phase currents at low speed and to sinusoidal phase currents above 1500 rpm, in general.
- Only when a fourth leg (connected to the null point) in the diode rectifier is added, does a notable third harmonic reoccur in the phase currents. The third harmonic in the phase voltage is reduced, however.
- In preliminary modeling, for transients, only the fundamental components of voltage and current may be considered. Even in this case, the equivalent magnetic saturation influence on L_d and L_q inductances should be considered.
- Comprehensive (3D) MECs or FEMs are required for a pertinent description of steady-state performance of the Lundell machine with diode rectifier and battery backup DC loads. The 3D trajectory of the main flux path reclaims such a treatment.
- The power factor angle (for the fundamental components) φ_1 increases from 1° at low speeds to up to 10° at high speeds due to diode commutation-caused reactive power “consumption” in the Lundell machine.
- The maximum DC output current increases steadily with speed for a given battery voltage.
- The four-legged diode rectifier leads to notable increases in the DC output current only at high speeds when it is not badly needed.

- The loss components in the Lundell machine are stator copper loss, stator iron loss, diode rectifier loss, rotor claw-pole eddy current loss, DC excitation loss, and mechanical loss. Only stator core loss and excitation loss tend to decrease above a certain speed. All the other components increase notably with speed.
- At 14 V_{dc}, the diode rectifier losses are about as large as the stator copper losses. Increasing the DC bus voltage to 42 V_{dc} is a way to notably decrease the relative importance of the rectifier loss, besides decreasing the wire size in the stator coils and the power equipment cable cross-section (and costs).
- Claw-pole geometry can be optimized for maximum DC output current, and a claw-pole span coefficient of around 0.45 in the axial middle of the stack was found to be best.
- Adding permanent magnets to boost performance for all speeds (DC output current) was proven to be effective only if the former are placed between the rotor claw to reduce the excitation flux leakage.
- Increasing the number of poles from 12 to 18, for the same stator and rotor overall size and stator slot total area, leads to a steady increase in maximum DC output current for given voltage, especially at high speeds. Losses and cooling aspects should be closely monitored.
- Winding reconfiguration (tapping or star to delta), with a larger number of turns in series at low speeds and a smaller number of turns at high speeds, causes an increase in output for all speeds. Again, losses and cooling aspects should be considered carefully.
- Enforcing the damper capability of the solid-iron claw poles on the rotor by placing aluminum plates between the claws also produces some DC output current increases for all speeds, because the commutation (subtransient) inductance L_c of the machine is reduced.
- Combining winding tapping with controlled rectifier (for low speeds) and the diode rectifier for high speeds produces remarkably higher output at all speeds. At low speeds, the controlled rectifier allows the machine to be partly magnetized from the battery, and thus, voltage power angle δ_v may increase up to almost 90° to extract the maximum available power from the Lundell machine. Adding a single IGBT DC–DC converter to the diode rectifier may produce output increases almost similar to those obtained with a controlled rectifier [19].
- The Lundell machine is also suitable as a starter–generator in mild hybrid vehicles with a 42 V_{dc} power bus.
- Motoring is required for starting the vehicle and at idle stop (ICE shutdown) to drive the air conditioner's compressor and other auxiliaries through a single ribbed-belt transmission.
- Generating is required during normal car driving and during vehicle braking.
- A DTFC scheme is introduced for motoring, and — after adequate new settings — for generating. Also, the DTFC scheme may be adapted for field-current-only control during generating, to increase reliability.
- Further efforts to increase the Lundell machine performance in terms of power, volume, higher speeds, efficiency, noise, vibration, and cooling are underway both for standard and mild hybrid vehicles.

References

1. I.G. Kassakian, H.-C. Wolf, J.M. Miller, and C.J. Hurton, *Automotive electrical systems circa 2005*, *IEEE Spectrum*, 33, 8, August, 1996.
2. G. Hennenberger, and J.A. Viorel, *Variable Reluctance Electrical Machine*, Shaker Verlag, Aachen, 2001.
3. I. Boldea, and S.A. Nasar, *Induction Machine Handbook*, CRC Press, Boca Raton, FL, 2001.
4. V. Ostovic, J.M. Miller, V. Garg, R.D. Schultz, and S. Swales, A magnetic equivalent circuit based performance computation of a Lundell alternator, *Record of IEEE–IAS 1998, Annual Meeting*, Vol. 1, 1998, pp. 335–340.
5. V. Ostovic, *Dynamics of a Saturated Electric Machine*, Springer-Verlag, New York, 1989.

6. R. Block, and G. Hennenberger, Numerical calculation and simulation of a claw-pole alternator, Record of ICEM-1992, Vol. 1, 1992, pp. 127-131.
7. G. Hennenberger, S. Küppers, and I. Ramesohl, Sensorless permanent-magnet brushless D.C. and synchronous motor drives, *Electromotion*, 3, 4, 1996, pp. 165-177.
8. R. Wang, and N.A. Demerdash, Computation of load performance and other parameters of extra-high-speed modified Lundell alternator from 3D-FE magnetic field solutions, *IEEE Trans.*, EC-7, 2, 1992, pp. 342-361.
9. S. Küppers, Numerical Method for the Study and Design of A.C. Claw-Pole Generator, Ph.D. thesis, RWTH, Aachen, 1996 (in German).
10. K. Xamazaki, Torque and efficiency calculation of an interior PM motor considering harmonic iron losses of both stator and rotor, *IEEE Trans.*, MAG 39, 3, pp. 1460-1463.
11. A. Munoz-Garcia, and D.W. Novotny, Utilization of third harmonic-induced-voltages in PM generators, Record of IEEE-IAS 1996 Annual Meeting, Vol. 1, 1996, pp. 525-532.
12. A. Mailat, D. Stoia, and D. Ilea, Vehicle alternator with improved performance, Record of Electromotion-2001, Bologna, Vol. 2, 2001, pp. 511-514.
13. F. Liang, J. Miller, and X. Xu, A vehicle electric power generation system with improved power and efficiency, *IEEE Trans.*, IA-35, 6, pp. 1341-1346.
14. C. Kahler, and G. Henneberger, Calculation of the mechanical and acoustic behavior of a claw pole alternator in a double and single star connection, Records of Electromotion-2001 Symposium, Bologna, Vol. 2, pp. 553-558.
15. T. Teratani, K. Kurarnoki, H. Nakao, T. Tachibana, K. Yagi, and S. Abae, Development of Toyota mild hybrid systems (THS-M) with 42 V power net, Records of IEEE-IEMDC-2003, Madison, WI, 2003, pp. 3-10.
16. I. Boldea, and S.N. Nasar, *Electric Drives*, CRC Press, Boca Raton, FL, 1998.
17. O. Pyrhönen, Analysis and Control of Excitation, Fields Weakening and Stability in Direct Torque Controlled Electrically Excited Synchronous Motor Drives, Ph.D. dissertation, Lappeenranta University of Technology, Finland, 1998.
18. C. Lascu, I. Boldea, and F. Blaabjerg, Very low speed sensorless control of induction motor drives without signal injection, Record of IEEE-IEMDC-2003 Conference, Madison, WI, 2003, 1395-1401.
19. D.J. Perreault, and V. Caliskan, Automotive power generation and control, *IEEE Trans.*, PE-19, 3, 2004, pp. 618-630.